



Proceedings

ASPE 2017 Spring Topical Meeting

Precision Engineering and Optics: What are the Limits of Precision, and How to Characterize Them?

April 24-25, 2017

American Society for

Precision Engineering

P.O. Box 10826, Raleigh, NC 27605-0826
(919) 839-8444 | Fax (919) 839-8039

Proceedings of

2017 Spring Topical Meeting

on

Precision Engineering and Optics:
What are the Limits of Precision, and How to
Characterize Them?

April 24-25, 2017

Westward Look Wyndham Grand Resort & Spa
Tucson, Arizona, USA

The American Society for Precision Engineering (ASPE) is a multidisciplinary professional and technical society concerned with research and development, design, manufacture and measurement of high accuracy components and systems. ASPE activities encompass relevant aspects of mechanical, electronic, optical and production engineering, physics, chemistry, and computer and materials science. Membership is open to anyone interested in any aspect of precision engineering.

Founded in 1986, ASPE provides a new focus for a diverse but important community. Other professional organizations have covered aspects of precision engineering, always as a sideline to their principal goals. ASPE is based on the core of generic concepts necessary to achieve precision in any application; independent of discipline, ASPE intends to be the focus for precision technology, and to represent all facets from research to application.

ASPE — The American Society for Precision Engineering
301 Glenwood Avenue, Suite 205, Raleigh, NC 27603
P.O. Box 10826, Raleigh, NC 27605
Telephone: (919) 839-8444 Fax (919) 839-8039
Web site: aspe.net

Preface

This book comprises the proceedings of the 2017 Spring Topical Meeting, *Precision Engineering and Optics: What are the Limits of Precision, and How to Characterize Them?* The contributions reflect the authors' opinions and are published as presented to ASPE without change. Their inclusion in this publication does not necessarily constitute endorsement by the ASPE or its editorial staff.

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Contents

Preface	ii
Contents	iii
Foreword	iv
Sustaining Corporate Sponsors.	v-vi
Corporate Sponsors.	vii
Schedule	viii
Meeting Program	ix-xi
Technical Papers	1-109
Author Index.	110

Foreword

Welcome to the ASPE 2017 Spring Topical Meeting, *Precision Engineering and Optics: What are the Limits of Precision, and How to Characterize Them*, at the Westward Look Wyndham Grand Resort & Spa in Tucson, Arizona, USA. Located in the Sonoran Desert, The Westward Look Wyndham Grand Resort and Spa is an oasis that offers an upscale Southwestern experience with modern conveniences and very close to some of Tucson's best attractions. With over 350 days of sunshine each year, the weather in Tucson is perfect for lots of outdoor activities like horseback riding and hiking. National parks and forests and the desert provide opportunities to explore the finest nature has to offer! Tucson has a very U. S. western feel, including U.S. Mexican and Native American influences. The University of Arizona provides a college-town atmosphere, while the city offers a fine array of museums and dining, shopping and a world-class golf resort.

This Topical Meeting will look at how well optics can be made and how to describe precision, particularly as a function of spatial scale of the optic or optical system. Because there is a partially accepted formalism for describing precision in optics by means of a power spectral density, or structure function, where precision is expressed as a function of the fractional size of the optic, can this same formalism be applied to other areas of precision engineering? Using precision in optics as a baseline, how do other fields of precision engineering stack up with methods of manufacture and metrology of optics?

The idea for this meeting's theme is an outgrowth of the Stedman diagram of the early 1990's for characterizing metrology instruments in terms of their linear measurement range versus their slope measuring sensitivity. Can the same sort of comprehensive idea describe the limits of precision in terms of a metric based on the scale of features of the object in question? Further, how much of what is known about the precision of the object is inherent in the object and how much is a limitation of the metrology as a function of scale?

Although optics are the main emphasis of this Topical meeting, it is intended to be multi-disciplinary with examples of the limits of precision and means for describing and measuring them coming from all the disciplines in precision engineering. Ideas of how to measure and describe precision in one discipline may have applicability in another yet be rather unknown or in-applicable in another. The goal of this meeting is to bring ideas from many fields together in a small group setting so the ideas can be discussed in depth concerning limits to precision based on design, materials, assembly, fabrication techniques and metrology.

Meeting Co-Chairpersons:

Robert E. Parks, Optical Perspectives Group, LLC

James E. Burge, University of Arizona

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MEETING SCHEDULE

	Monday April 24, 2017		Tuesday April 25, 2017
7:30 AM	Registration Check-In & Continental Breakfast Quail Room	7:30 AM	Registration Check-In & Continental Breakfast Quail Room
8:30 AM	Welcome Remarks & Session 1 – Technology Quail Room <i>(breaks will be as needed)</i>	8:30 AM	Session 3 – Limits of Precision Quail Room <i>(breaks will be as needed)</i>
11:30 AM	Group Lunch Javelina Room	11:30 AM	Group Lunch Javelina Room
1:00 PM	Session 2 – Hardware Quail Room <i>(breaks will be as needed)</i>	1:00 PM	Session 4 – Spatial Scale of Precision Quail Room <i>(breaks will be as needed)</i>
		3:00 PM	
		3:30 PM	Discussion Session Quail Room
4:00 PM			
4:30 PM	Bus Leaves for Mirror Lab <i>(meet in hotel lobby)</i>		
5:00 PM	Mirror Lab Tour Begins	5:00 PM	END CONFERENCE
6:30 PM	Cocktails and Dinner at the University Stadium Club <i>(bus will leave between 8:30 PM–9:00 PM to return to Westward Look)</i>		

Meeting Program

Monday, April 24, 2017

7:30 AM

Registration Check-In (Just Outside of Quail Room)

7:30 AM – 8:30 AM

Continental Breakfast (Quail Room)

8:30 AM – 11:30 AM

Welcome Remarks & Session 1

Technology (Quail Room)

1. Welcome and Overview of Precision Engineering and Optics

Robert E. Parks, Optical Perspectives Group, LLC 1

2. Manufacturing of Precision Optics on Non-Precision Machine Tools

Medicus, K. M.; Lynch, T.; Hyman, M.; Ferralli, I. (Optimax Systems Inc.). 4

3. Thin Optical Window Measurement with a Spectrally Controlled Interferometer

Salsbury, C.; Olszak, A. (Apre Instruments) 9

4. Improvement of the Angular Precision of an Optical Metrology Platform by Disturbance Compensation

Riel, T.; Galffy, A.; Sinn, A.; Schitter, G. (TU-Wien) 15

5. Scalability in Contact Lens Manufacturing to Improve the Refractive Performance and User Comfort

Ellis, J. D.; Brooks, D. R.; Butler, S.; Zheleznyak, L. A.; Gandara-Montano, G. A.; Wozniak, K.; Knox, W. H. (University of Rochester). 21

6. The History and Future of the Stedman Diagram

Blum, A.; Evans, C. J. (University of North Carolina-Charlotte) 26

11:30 AM – 1:00 PM

Group Lunch (Javelina Room)

1:00 PM – 4:00 PM

Session 2

Hardware (Quail Room)

1. MEMS Deformable Mirrors for Adaptive Optics

Bifano, T. G. (Boston University) 32

2. 4DoF Active Optical Alignment Tool

Haase T.; Guddei, B.; Diez, P. (Physik Instrumente (PI) GmbH & Co. KG) 37

3. Tip-Tilt Vibration Compensation: A System Analysis

Sinn, A.; Riel, T.; Deisl, F.; Schitter, G. (TU-Wien) 42

4. Extreme Stability of a V-shaped Cavity Optical Feedback Laser Source Jobert, N. (Synchrotron SOLEIL); Casado, M.; Landais, A. (Laboratoire des Sciences du Climat et de l'Environnement); Kass, S.; Stoltmann, T. (Université Grenoble Alpes / CNRS)	48
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4:30 PM

Bus leaves for University of Arizona (Meet in Lobby of Hotel)

5:00 PM

Arrive at University of Arizona. Proceed to Auditorium to view film
on Mirror Casting. Mirror Lab Tour will begin.

6:30 PM

Cocktails and Dinner at the University Stadium Club

8:30 PM – 9:00 PM

Bus leaves to return to Westward Look

Tuesday, April 25, 2017

7:30 AM

Registration Check-In (Just Outside of Quail Room)

7:30 AM – 8:30 AM

Continental Breakfast (Quail Room)

8:30 AM –11:30 AM

Session 3

Limits of Precision (Quail Room)

1. Angular Measurement to 0.1 Arc Seconds: Gaithersburg Latitude Observatory circa 1900 Rhorer, R. L. (Rhorer Precision Engineering LLC)	55
2. Stuck in a Moment: A View from the MIRE Egan, P.; Stone Jr., J. A.; Ricker, J.; Hendricks, J. (National Institute of Standards & Technology)	66
3. Limits to Resolution in Optical Dimensional Metrology de Groot, P. J.; Deck, L. L.; Badami, V. G. (Zygo Corporation)	72
4. Fizeau Interferometers: The Best Nulling Autocollimators Money can Buy? Evans, C. J. (University of North Carolina-Charlotte); Ruijl, T. A. M. (MI-Partners); Holmes, M. L.; Deck, L. L.; Soobitsky, J. A. (Zygo Corporation).	77
5. Characterization of Advanced LIGO Core Optics Billingsley, G.; Yamamoto, H.; Zhang, L. (California Institute of Technology).	78

11:30 AM – 1:00 PM

Group Lunch (Javelina Room)

1:00 PM – 3:00 PM

Session 4

Spatial Scale of Precision (Quail Room)

1. High-Precision Lateral Distortion Correction in Coherence

Scanning Interferometry using an Arbitrary Surface

Su, R.; Leach, R. K. (University of Nottingham) Ekberg, P.

(KTH Royal Institute of Technology) 84

2. Characterization and Optimization of Ultra-Precision Machine Tools

and Processes by Time Resolved PSD Signature of Micro/Nano

Structured Optical Surfaces

Uhlmann, E.; Kühne, S.; Jagodzinski, M.; Malcher, M.

(Technische Universität Berlin) 90

3. Surface Slope Error Distribution on Precision Optical Surfaces

Girard, L.; Kincade, J.; Marchetti, L. (ZYGO Extreme Precision Optics) 96

4. Revisiting PSD Analysis Considerations

Creath, K. 100

5. Computer Generated Holograms as 3D Calibration Artifacts

Zhou, C. (Arizona Optical Metrology); Parks, R. E.

(Optical Perspectives Group, LLC). 105

3:30 PM – 5:00 PM

Discussion Session (Quail Room)

END CONFERENCE



Technical Papers

Overview of Precision Engineering and Optics Meeting

Robert E. Parks
Optical Perspectives Group, LLC
Tucson, AZ 85750

INTRODUCTION

As an introduction to this meeting on Precision Engineering and Optics, I would like to give you an insight to my thoughts as I perceived the program for the meeting and the scope of the content. The first idea was to try to get across the idea that optical concepts and instruments are useful, if not fundamentally imperative, to precision engineering now and in the future. Advanced methods of manufacture are now precise to the limits we associate with the wavelength of visible light, about $0.5\ \mu\text{m}$, and to do the needed metrology to that level optical methods are needed to replace traditionally mechanical methods.

The second concept has to do with astronomy. For one, we are in a city of astronomers and we should be aware of what they are doing and can do in the way of precision. It is one of my greater regrets that I was not able to get an astronomer to speak at this meeting, most saying they were simply over worked and had no time. I will try to give a bit of background on why I think astronomers and astronomy are important to our field.

Finally, there is the question of fundamental limits to precision, and the question, as precision improves, how we separate the errors in the metrology instrument from the errors in the measurand. Several papers in the program address these questions, and we are particularly pleased to have a report on the very fine optics that went into the LIGO project that produced the first observation of a gravitational wave. Other papers discuss variations in precision as a function of spatial scale and novel uses of familiar optical techniques.

IMPORTANCE OF OPTICS

On the first topic, the importance of optics in precision, take the simple task of finding the center of a ball. First, recognize a ball is a physical realization of a theoretical point in space and that locating points is the fundamental function of physical metrology. The

traditional method of finding the center of a ball is to use a tactile probe, touch the surface in at least 4 points and fit the touch points to the equation of a sphere to determine the center. The method is tedious, indirect and semi-static.

Using a simple optical instrument called an autostigmatic microscope, the center of a ball can be found by a non-contact means precisely in two dimensions while scanning past the ball, and with a simple modification to the microscope, in all three dimensions while scanning. If greater precision is required an interferometer can be used to find the ball center in 3 dimensions to nm precision while additionally giving a vast quantity of information about the local shape of the ball surface. The paper on using an interferometer to measure straightness is an illustration of this idea in 2 dimensions.

The message is that the touch probe approach is a serial method of measurement while the optical method is analogous to parallel processing. Because of the parallel data capture and processing, vastly more information can be gathered about the measurand and the information is gathered faster.

ASTRONOMY

Turning to astronomy as an illustration of precision techniques, on occasion I have been a skeptic of astronomy. As a global community we spend billions on astronomy and what do we get for it besides pretty pictures? As a part of organizing the program for this meeting I recalled a 1984 article in Scientific American (Vol. 250, Issue 4, p. 54) that made a great impression on me back then. The article was about using diamond anvils as a way of creating huge pressures on samples.

A part of the article was a graph of the range of pressures on a logarithmic scale we could achieve here in the laboratory on earth from about 10^{-8} for the best mechanical vacuum pump to 10^6 atm with the diamond anvil. That range was the central roughly one third of the

scale total scale that went from 10^{-32} atm for the pressure of hydrogen in intergalactic space to 10^{30} atm at the center of a neutron star. This says that roughly 2/3rds of all observable experiments in the Universe are going on in space and the only way of getting data from these experiments is through remote sensing via astronomy.

Another incident related to astronomy happened as I was thinking about the meeting. I had the chance to visit the US Naval Observatory in Flagstaff, Arizona, a place not that easy to visit. During the tour, our guide Dr. Merri Sue Carter, said their main function at the Observatory was to measure the precise locations of stars for navigational purposes, and they did this by triangulation from different observatories with large distances between them. One of the problems with doing this was that some observatories were on tectonic plates that moved enough to throw off some of their measurements. Only some observatories were stable enough for good measurements. She wished there were some observatories in central Africa and Siberia to fill in gaps in their data.

While trying to find an astronomer who would speak I looked at some of the data on the precision of astrometric measurements, and the graph in Fig. 1 is illustrative. Notice the log scale and the number of stars (50×10^6 with a precision of 10 micro-arcseconds) observed by GAIA, a European Space Agency satellite, using a stellar interferometer to make the observations. This illustrates the extreme precision that can be obtained with optical interferometry, the beauty of a space environment with no air and little vibration and an active alignment system for

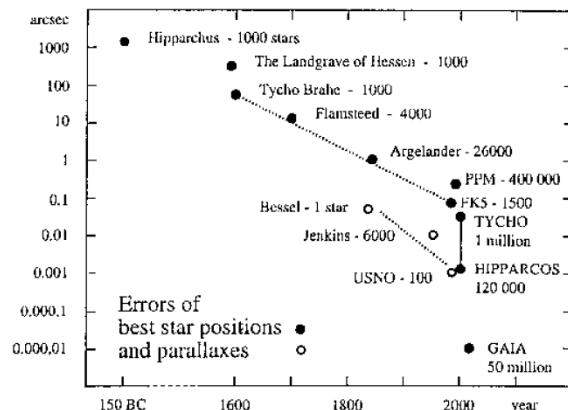


Fig. 1 From Lindegren and Perryman, *Astron. Astrophys. Suppl. Ser.* **116**, 579-95 (1996)

keeping the two arms of the interferometer fixed relative to each other. It is also a beautiful illustration of the parallel data collecting and processing afforded by optical methods.

Besides the precision achievable in astronomy there are the astronomers themselves. They are a vast treasure of knowledge about precision instruments. They largely keep to themselves as a community but share information and results very freely. Because most of the rest of practicing precision engineers know little about astronomy or astronomers this body of knowledge goes largely untapped to the best of my awareness. My suggestion is that if you know any astronomers, use them to bounce ideas off of. They are practical people who never have enough money to do the observations they want, yet they find pragmatic ways to accomplish their goals.

In one area they have been quite successful and that is pushing the sensor manufacturers to improve and make larger focal plane arrays, and to expand the wavelength regions to which these arrays are sensitive.

Additionally, universities graduate far more students with degrees in astronomy than there are positions for astronomers. Yet most of these students have a solid background in physics, engineering and software development. They represent good candidates for recruitment.

LIMITS OF PRECISION

Finally there is the area of the limits of precision and how well a particular measurement can be made. First there are some fundamental physical limits as I expect the LIGO optics paper and others will speak to. Then there is the type of and dimension of the measurement to be made. Others have probably explained this better than me, but here are some thoughts. The simplest measurement is counting because if you have integers from 1 to 100, you have precisely 100 integers with no error. This next measurement in terms of complexity is the duration of time or measuring frequency where the time intervals are fixed to great precision (1 part in 10^{16}), but the uncertainty comes with an error in the first and last counts so the answer can be written as $xxx \pm 1$ or 2.

Next in difficulty is the measurement of length where in addition to uncertainty at either end of the measurand is the uncertainty in the scale of

the measurement. This is seen daily in the specifications for scales and any one axis on a CMM. There is a fixed error plus an error that is proportional to the length of the measurement. Now the result is written as $xxx \pm (yy + a \cdot xxx)$.

When profile is added a whole new set of uncertainties arise. There is a reference profile against which the measurand is compared; there are the “end” uncertainties and scale errors perpendicular to the profile and possible Abbe errors. In addition, there is the spatial and angular resolution of the probe meaning there is a limit to how finely the profile may be determined along its length as will be noted in the paper on the Stedman diagram. On the other hand, there are also tools such as reversal and PSD techniques to help determine how well the profile can be ascertained as a function of the length of the profile.

Obviously the difficulty of making measurements in the additional one and two degrees of freedom compound the potential errors sources as now the degree to which the axes are orthogonal impacts the results of the “profile” measured in each of the three axes.

It is not that we are helpless. There are ways of backing out some of the errors; comparison with physical artifacts, reversal techniques and statistical methods. Every effort is being made to get rid of physical artifacts as basic standards, but sometimes physical artifacts are the best day-to-day ways of assuring precision in machine set up and metrology. Another advantage is there is a piece of hardware on which to place a calibration sticker to satisfy the bean counters.

Reversal techniques are perhaps the most universal and reliable techniques for calibration but are often mis-understood by quality inspectors, and unrecognized by many standards organizations as a means of verifying precision. Further, these methods are often tedious and difficult to implement well. A third means of calibration, useful in certain cases, are statistical methods such as are used to calibrate reference surfaces in surface roughness interferometers by averaging a number of uncorrelated patches of a smooth surface. Again, a process that is somewhat tedious to perform well, sometimes not well understood and only applicable in certain situations.

CONCLUSION

These are some of the aspects of precision to keep in mind as you read the papers from this meeting. This meeting is a unique opportunity to hear of a rather disparate ensemble of optical techniques intended to improve precision. Hopefully there are techniques from a field distant from yours that can be applied profitably to your concerns. As well, we want to thank the authors who are presenting for taking the time to share with others their insights to improving precision by way of optical techniques.

Please take the opportunity to introduce yourself to unknown faces, and see if there may be areas of overlap in your mutual fields of expertise that may help you in your pursuit of precision. My hope is that you will make the most of this opportunity.

Manufacturing of Precision Optics on Non-Precision Machine Tools

Kate Medicus, Tim Lynch, Mike Hyman, and Ian Ferralli

Optimax Systems Inc, Ontario NY, 14519

INTRODUCTION

The definition of 'precision' is not as precise as one might imagine. Some have considered the definition to be parts in 10^5 . A rough example of that definition is a stage moving 1 m with the ability to accurately step in $10\text{ }\mu\text{m}$ increments ($1\text{e}6\text{ }\mu\text{m} / 1\text{e}5$). It is clear that there are many precision machine tools that are able to achieve levels of precision better than 1 part in 10^5 , but in general, it is at this level that the precision engineering techniques and principles must be considered.

In the optics industry, 'precision' surfaces have been achieved for many years. Following the same precision definition, a precision optical surface would have irregularities (deviation from perfect, FIGURE 1) less than the lens diameter divided 10^5 . For a typical lens of 50 mm in diameter, the irregularity would be $0.5\text{ }\mu\text{m}$, about 1 wave in optic parlance ($1\text{ }\lambda = 0.6328\text{ }\mu\text{m}$). It is clear that optical surfaces with better than $1\text{ }\lambda$ peak-to-valley (PV) irregularity have been manufactured for many years, even before the advent of precision interferometer measurement tools (Newton's rings).

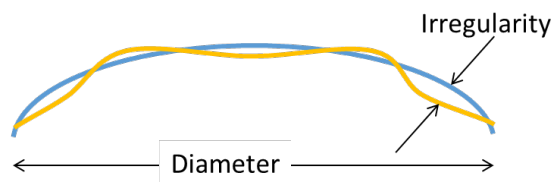


FIGURE 1: Sketch showing the irregularity peak-to-valley error on an optical surface.

Many in the precision engineering community would say that a precision surface with less than $1\text{ }\lambda$ of PV error would need to be manufactured on a precision machine tool, such as a diamond turning machine with precision positioning axes, spindles, and feedback. And in many cases, this is done with diamond turnable materials (aluminum, electroless nickel, etc.). Currently it is possible to procure commercially diamond turned optical surfaces from metals and some IR

crystal materials (zinc-sulfide, etc.). But, the vast majority of refractive optical glass and ceramic materials are not diamond turnable. (There is some research in this area, but it is not a fully viable solution at this time.)

How is it possible that precision, even ultra-precision ($\lambda/100$, parts in 10^6 or 10^7) optical surfaces are commercial products, when precision machine tools are not viable? This is the central premise of this paper, that precision optics can be manufactured on non-precision machine tools.

This topic has been briefly explored by Roher and Evans who summarized "the differences between diamond turning and traditional optical fabrication ... by describing diamond turning as a displacement - controlled process versus a force - controlled process for traditional optical fabrication" [1]. We would add a supposition that that modern optical fabrication has added time-control to the process to achieve even higher precision than what was possible with just force control.

Polishing is a time dependent process; it is different than the material removal process of diamond turning, where it is a positioning based process. In machining, the workpiece material will be removed by the tool (two objects cannot occupy the same space). This is the reason for the high-stiffness needs of precision machine tools – so that the material and tool don't just push each other away. In polishing, the 'tool' is an area of micrometer sized particles abrasive that has compliance against the workpiece. The more time that the particles spend on the part, the more material removal will occur.

OPTICAL SURFACE MANUFACTURING

A typical precision optical surface is manufactured using the following steps (in general):

- 1.) micro-grinding with diamond grinding tools (brittle material removal) on a multi-axes (non-precision) machine tool,

- 2.) (optional) loss abrasive grinding (brittle material removal) to remove sub-surface damage from step 1; probably with hand motions
- 3.) pre-polish (ductile material removal) to remove sub-surface damage from steps 1 and 2, on a simple rotating spindle for spherical optics and a non-precision machine tool for aspheric optics, and



FIGURE 2: The simple spindle with overstroke for the pre-polish stage [2].

- 4.) (optional) figure correction to achieve final figure specifications with a small tool polisher (ductile material removal), non-precision machine tool.

Step 4 is optional based on the final specification; precision surfaces can still be achieved using the pre-polish methods. None of the machines used in the polishing process (even for the final figure correction) have any of the traditional aspects of a precision machine tool such as displacement interferometers, high stiffness, and air-bearing/hydrostatic spindles.

TRADITIONAL POLISHING

Step 3 for spherical and plano optics is the traditional method of pitch polishing (FIGURE 2) where a pitch lap of the same general shape of the part is moved relative to part with a overstroke of some type. The overstroke is not a precision motion and the spindle rotating the part (or tool) is not precision. This is the force controlled process described by Rhorer and Evans.

FIGURE 3 [3] shows a framework describing the variables that affect the material removal rate in

polishing using Preston's equation where material removal rate is proportional to Preston's coefficient, C , times, the pressure, P , times the sliding speed, U , between the part and lap. It is clear that precision control of all of the variables in this process would be a herculean task, though some of the industry has found success in moving towards more precision control.

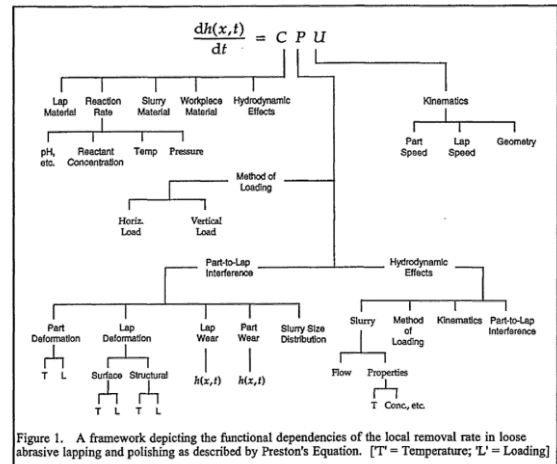


FIGURE 3: An overall description of all the variables in the traditional optical polishing process [3].

Two aspects of this polishing process that allows for precision surfaces is randomization and averaging. The pitch lap is shaped to be of similar shape of the part and is then moved over the part surface in a (mostly) random pattern in the presence of slurry. The random pattern ensures that all areas of the pitch lap touch all areas of the part. This allows surface figure errors in the pitch lap and in the part surface to counter each other and smooth each other out. The averaging effect ensures that the figure error will converge to a spherical (or planar) surface. It is partly the movement of the pitch lap that allows for this averaging – pitch can smooth out to match the part shape. This is the cause of the phasing 'the table is running tenth wave' – if the pitch has achieved a good surface flatness, the parts will converge to that level also.

During polishing, the optician will check the part's surface irregularity using interferometry or test plates to confirm that the surface figure error is as desired. If the irregularity is not converging; it may be that the randomization is not correct (or one of many other variable problems). The optician can change the randomization to affect the material removal rate in a specific zone of the part to correct a surface irregularity.

Outcomes of this traditional polishing process can be to the sub-precision level, to better than $\lambda/10$ in many cases, all on non-precision machine tools.

MODERN POLISHING

While the optician has some control over form errors in the traditional polishing process, it is not deterministic and it is extremely difficult to remove material in at a specific location on the optic's surface. (Though there are many stories about polishing legends who could hand polish an optic to perfection.)

Polishing, specifically for very high precision and for aspheric and freeform optics, has moved to a sub-aperture polishing method. A small (size is variable) tool is moved over the optic's surface under some sort of computer control. These type of polishing machines perform both 'gray-out' (the damage removal after generation) and figure correction (selective material removal rates).

The polishing machines are available commercially from multiple suppliers: QED Technologies, OptiPro, SatisLoh, OptoTech, Zeeko, Schneider, custom company-specific tools, and others. While we have not examined all of the polishing machine tools from these suppliers, we have had the opportunity to operate many of them and have interacted with the sales groups from these companies. Hands-on examination and information from the supplier's themselves indicate that these CNC sub-aperture polishing tools are not 'precision' tools by the parts in 10^5 definition. These tools do not require or ask for strict temperature control, they have no air bearing spindles, there are no displacement measuring interferometers on the axes for feedback, there appears to be no correction of axes errors, etc. The polishing tools do not have the ability to position to sub-micrometer level accuracies, but are capable of producing surfaces with tens to ones of nanometer irregularity errors. This is done because of three reasons: precision measurements, dwell based corrections, and polishing spot resilience.

Dwell Based Corrections

As discussed earlier, polishing is a time dependent process; the more time the polishing abrasive spends on the workpiece, the more

material will be removed. This is the basic principle of the dwell based correction algorithms. During the dwell correction, the tool/workpiece relative velocity will be slower at areas that require more material removal and faster at the areas where less material removal is required. Required for this type of correction is a surface measurement, indicating where material removal should occur.

In the lower precision application of dwell correction, the nominal and range of velocities is decided by optician experience. In this lower precision application, it is probably necessary to inch up to the correction, to try to do only a little bit at a time. This is to prevent an over-correction (the highs became low and the lows became high).

In the higher precision application of dwell correction, a measurement of the tool's influence function is done. The tool influence function is the material removal function with a specific shape, size, and depth dependent on tool configurations, material type, slurry type, etc. and time. A convolution between the tool influence function with the measurement will result in a more exact velocity map. With experience, a good measurement, and a stable tool influence function, one shot corrections are feasible to bring a part into specification.

The accuracy of the dwell based correction is dependent on the stability of the polishing spot size, the measurement accuracy, and mapping between part, machine, and measurement coordinate systems.

The resolution of the dwell correction is the smallest height difference that can be corrected, not the lateral spacing. The smallest resolution is dependent of the smallest velocity change that can be affected. In general, material removal rates for sub-aperture figure corrections are very small, so the smallest velocity change feasible in a non-precision machine tool is still enough for nanometer level corrections.

Dwell based corrections have the possibility achieving of $\lambda/60$ or better surface irregularities.

Precision Measurements

Inherent in the dwell based correction is the precision measurement. This is only part of the polishing process that can be considered 'precision' by the traditional definition. A

measurement to levels better than the final specification is required to achieve that final specification. This is not just for verification, but the precision measurement is required to feed the dwell based correction. The interferometric measurement has all the typical precision requirements that you expect: tight temperature control, no vibration, calibration of the reference artifact, etc.

In most cases, the measurement is accomplished through figure measuring, phase shifting interferometry. A sub-aperture figure measuring stitching interferometer is sometimes used for aspheres and larger diameter optics. These stitching interferometers are not precision machine tools either, but through measurement overlap, good calibration, and robust algorithms, precision level measurements are possible.

The measurement must be done with the part in the unstressed state (or the at-use stressed state). While this is important for the measurement, it is not as important for polishing.

Polishing Spot Resilience

The polishing spot has a surprising amount of resilience in that it need not be positioned perfectly to achieve precision results. The sub-aperture polishing media has a compliance created in difference manners depending on the polishing tool. In general, we can consider that the part will 'plunge' into the polishing tool. The amount of plunge is chosen and set for multiple reasons: desired material removal, tool size, area of effect, and others. In the case of positioning errors, FIGURE 4, the polishing media will flex to a slightly different polishing spot because of the different plunge position. Based on experience, changes of less than ten percent of the plunge depth will not dramatically affect the correction precision. With a typical plunge depth near 1 mm, it is clear that precision level positioning is not required.

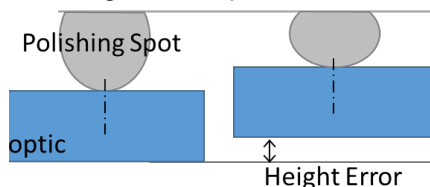


FIGURE 4: Examples of positioning error in sub-aperture polishing.

It is to our advantage that precision positioning is not required, not just because the machine tools do not need the care of a precision tool. Recall that the measurement must be taken in the non-stressed state, but the polishing correction does not need to be. For example FIGURE 5 shows a thin optic being deformed under vacuum loading, a typical method of mounting on a sub-aperture figure correction machine. This would deformation would give an extremely inaccurate result in measurement, but as long as the deformation is not huge, it is not a significant issue.

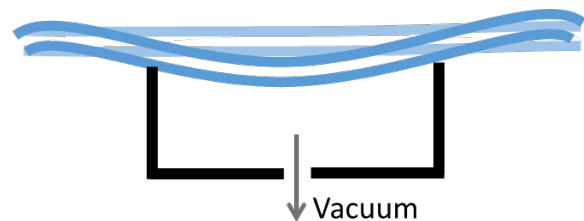


FIGURE 5: Example of part deformation under improper vacuum loading.

Another time where this resilience is advantageous is mild aspheres or freeforms. Typically, the machine tool must be told the nominal part shape to follow the optic surface during polishing. If the part has a mild aspheric or freeform departure, 10 μm to 100 μm , this step of inputting the exact shape can be skipped saving a little time and computation.

CONCLUSIONS

In conclusion, precision optics can be manufactured on non-precision machine tools because the material removal function is based on force and time, not on positioning accuracy. This is not to say that polishing couldn't be improved if more 'precision' techniques were implemented. There are some researchers investigating this area.

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THIN OPTICAL WINDOW MEASUREMENT WITH A SPECTRALLY CONTROLLED INTERFEROMETER

Chase Salsbury^{1,2}, Artur Olszak^{1,2}

¹ÄPRE Instruments

Tucson, Arizona, USA

²College of Optical Sciences

University of Arizona

Tucson, Arizona, USA

INTRODUCTION

The measurement of thin optical windows is a continuing challenge in the field of conventional interferometry. Reflections from both surfaces cannot be isolated and measured independently resulting in degraded overall accuracy. To remove unwanted reflections, one surface can temporarily modified and in extreme cases, treated with an anti-reflection coating. Here we present a new interferometric approach which addresses this challenge by producing low coherence fringes in a common path interferometer such as Fizeau through modulation of the source spectrum. Localized, high contrast fringes allow each surface of a thin optical window to be isolated for measurements without any surface treatment.

This method of Spectrally Controlled Interferometry (SCI) is a general approach to interferometry which can be applied to any interferometer type and combined with many fringe analysis methods and used to upgrade existing interferometric setups. The analysis and results presented in this paper will discuss a temporal phase shifting approach for simplicity and conciseness but other methods can be used as well.

THEORY

Optical coherence refers to the ability to produce interference fringes and is separated into two categories: spatial coherence and temporal coherence. In this paper, we discuss a method of manipulating the temporal coherence of the interferometer's source to achieve localized interference fringes at a predetermined location in a non-zero optical path difference (OPD) condition. In other words, we will present a general method of producing "white-light" fringes in common path interferometers such as Fizeau, thus combining the best of both worlds: localized fringes and high degree of error cancellation.

In general, fringe contrast, C , of two interfering beams is given by [1]:

$$(1) \ C = \frac{2\sqrt{I_1 I_2}}{I_1 + I_2} |\gamma|$$

In which, I_1, I_2 , refers to the intensity values of the two interfering beams measured at a detector and, $|\gamma|$, is the normalized coherence function which is related to the source spectrum and induced optical path difference between the beams. In an ideal measurement scenario, the intensity values of the two interfering beams are matched such that, $I_1 = I_2$ and the contrast equation above simplifies to, $C = |\gamma|$, but in reality this value is degraded by noise and imbalance of beam intensities.

From the Wiener-Khinchin Theorem [2], the normalized coherence function and power spectrum of an optical source, $S_u(\nu)$, are related to through the Fourier Transform, such that

$$(2) \ |\gamma(\tau)| = \left| \frac{F^{-1}\{S_u(\nu)\}_{\nu \rightarrow \tau}}{F^{-1}\{S_u(\nu)\}_{\nu \rightarrow \tau}|_{\tau=0}} \right|$$

Where, the temporal coherence variable, τ , can be converted to optical path difference by:

$$(3) \ OPD = \tau * c$$

For example, a Gaussian spectrum, with width, $\Delta\nu$, and central frequency, ν_0 , has a scaled Gaussian inverse Fourier transform with width, $\tau_c = \frac{1}{\Delta\nu}$, shown in Figure 1.

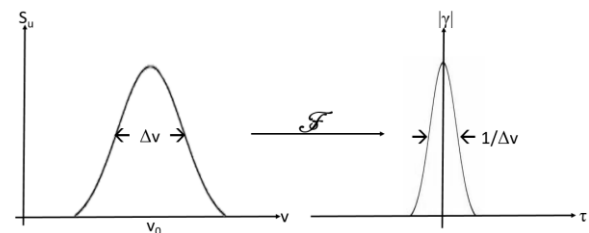


FIGURE 1. Inverse Fourier transform of a Gaussian power spectrum is a scaled Gaussian function coherence function.

A key component of finite bandwidth source interferometry is that fringe contrast is always unity in the null condition of $OPD = 0$ and fringe contrast decreases as OPD increases. The only condition in which the high contrast fringes can be detected is when the optical path difference is within the coherence envelope of, $OPD \leq \frac{c}{\Delta\nu}$. In the wavelength domain this expression for envelope width is written as, $OPD \leq \frac{\lambda_0^2}{\Lambda}$, in which λ_0 is the central wavelength and Λ , is the bandwidth.

However, if that same broadband spectrum is modulated by a cosine function, sidebands appear in the coherence function, as shown in equations (4),(5) below and Figure 2.

$$(4) f(\nu) = \exp\left(-\frac{\pi(\nu-\nu_0)^2}{\Delta\nu^2}\right) \left[\frac{1}{2} + \frac{1}{2}\cos(f\nu)\right]$$

$$(5) |\gamma(\tau)| = \exp\left(-\frac{\Delta\nu^2\tau^2}{\pi}\right) \otimes \left[\delta(\tau) + \frac{1}{2}\delta(\tau-f) + \frac{1}{2}\delta(\tau+f)\right]$$

These sidebands allow interference fringes to be produced and measured at non-zero optical path difference configurations.

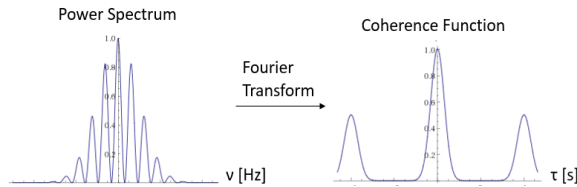


FIGURE 2. Inverse Fourier transform of a modulated Gaussian power spectrum is a scaled Gaussian function coherence function with 0.5 amplitude sidebands.

Three parameters control the measurement configuration. The first parameter is the coherence envelope of the nominal coherence function, as well as each sideband. Its width is controlled by the spectral width of the source, $\Delta\nu$. Second, by changing the frequency of the cosine modulation, f , the sidebands in the coherence function can be shifted in OPD space. This modulation frequency can be converted to a more convenient domain and written in terms of wavelength modulation period, $f = \frac{\lambda_0^2}{c\Delta\lambda}$. Using the

variable relationship in equation (3), the coherence function can be written as a function of OPD ,

$$(6) |\gamma(OPD)| = \exp\left[-\left(\frac{\Lambda}{\lambda_0^2}\right)^2 * \frac{OPD^2}{\pi}\right] + \frac{1}{2} \exp\left[-\left(\frac{\Lambda}{\lambda_0^2}\right)^2 * \frac{(OPD - \frac{\lambda_0^2}{\Lambda})^2}{\pi}\right] + \frac{1}{2} \exp\left[-\left(\frac{\Lambda}{\lambda_0^2}\right)^2 * \frac{(OPD + \frac{\lambda_0^2}{\Lambda})^2}{\pi}\right]$$

This allows controlled tuning of the cavity parameters through modulation of the spectrum alone with no mechanical alignment. Finally, the phase of the interference fringes is directly related to the phase of the modulation function. By maintaining a constant modulation frequency and shifting the relative phase of the cosine function, the interference fringes can be phase shifted for automated analysis.

APPLICATIONS AND ADVANTAGES

Spectrally Controlled Interferometry is a novel approach with advantages from both high coherence and low coherence interferometry. Because the distance at which the fringes are visible is tunable, surface isolation over a wide range of distances is achievable and measurements can be taken with no temporal coherent noise. Additionally, the phase shifting is controlled without mechanical actuation removing the need for mechanical calibration and providing quasi-unlimited phase shifting range at near perfect linearity.

While the advantages of SCI can be applied to a wide range of applications within the field optical metrology, only a few will be highlighted in this section and demonstrated in the sections to follow.

First, as mentioned previously, SCI removes unwanted reflections from multiple surfaces without any modification to the measurement alignment and more importantly to the surfaces themselves. The dynamic range of this surface selection is only limited to the ability to modulate the source spectrum and the nominal source bandwidth.

The speed at which different surfaces can be isolated is limited only to the switch-time of the modulation frequency. To illustrate, a measurement can be made at, $OPD = 50\mu m$ and reconfigured to measure, $OPD = 5mm$, in the very next frame without any mechanical realignment. While challenging from an optical

design perspective, this two order of magnitude dynamic range is achievable and in theory is not limited to larger ranges.

This enables the measurement of optical components with both large and small magnitude step functions or the measurement of optical systems that have already been assembled and require alignment between surfaces and/or to a reference surface.

One other feature is possible to facilitate optical measurements. SCI offers capability to not only tune the location of fringes but also the volume of space where fringes are visible by limiting the bandwidth of the source. This varying degree of temporal coherence can be particularly useful during the alignment stage which often is the longest part of the measurement process. By limiting the total source bandwidth it is possible to increase the depth of fringe visibility and in some cases provide non-localized fringes just as in conventional interferometry. Once the part is in position, the fringe envelope can be located on the measured surface and isolated from back reflections.

EXPERIMENTAL RESULTS

Thin Optical Window Measurement

To illustrate the ability to isolate surfaces on thin optical windows, a microscope slide was aligned to a Fizeau interferometer with an air gap of $50.8\mu\text{m}$. This air gap distance corresponds to a window thickness of $t_{\text{glass}} = \frac{50.8\mu\text{m}}{n_{\text{glass}}}$. The

experimental setup is shown in Figure 3.

In this experiment, there are four surfaces which reflect light and create multiple beam interference. The solid lines in Figure 3 correspond to the ray paths which will be isolated by matching the path difference to the fringe location of the SCI. The dashed lines represent ray paths which will not add coherently to the measurement.

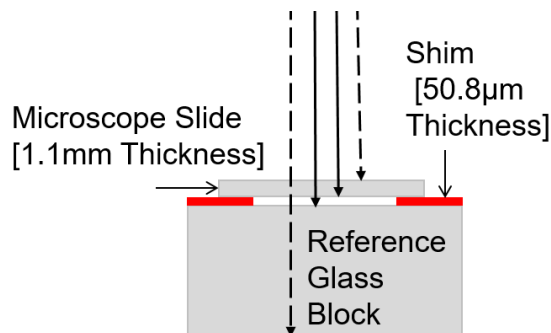


FIGURE 3. Experimental setup with a microscope slide separated by mechanical shims from reference surface. Solid lines represent reflections which correspond to the OPD configuration of the SCI. Dashed lines represent reflections which are not coherently added to measurement signal.

An example of the captured interferogram frames and the measured surface profile of the microscope slide are shown in Figure 4 and Figure 5 below. The surface measurement and analysis is performed in Apre Instruments' metrology software, Reveal™.

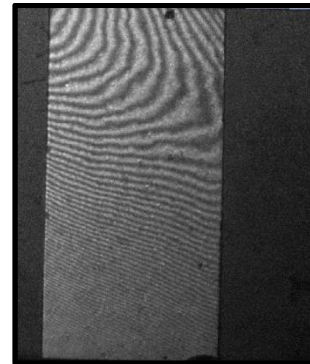


FIGURE 4. Single frame captured with high contrast fringes produced at OPD = 50.8 micron air gap with no coherent contributions from microscope slide first surface or secondary reference surface.

In Figure 4 above, high contrast fringes are produced at the desired OPD condition with no significant degradation of the coherent signal from the contributions of the microscope slide first surface and secondary reference surface.

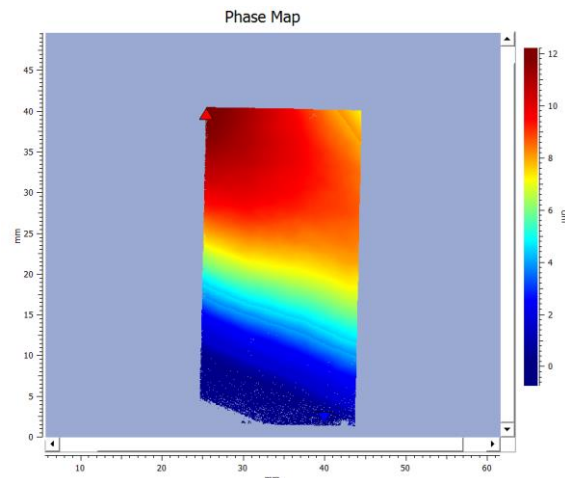


FIGURE 5. Measured phase map of microscope slide from interference fringes produced at 50.8 micron air gap.

Demonstration of Multiple Surface Selection and Measurement

To exhibit SCI's ability to reconfigure the surface selection and measurement, two microscope slides are aligned to a reference surface, as in the previous experiment. Shim stock was used to produce two target measurements separated by a larger OPD than the nominal coherence envelope of the source. The experimental setup is shown in Figure 6.

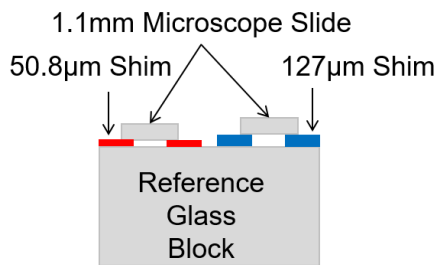


FIGURE 6. Experimental setup with two different target air gaps to measure. Each air gap is isolated independently and measured consecutively without any change to mechanical alignment.

This demonstrates the SCI's ability to operate in a white light interferometer-like mode without any mechanical translation. This method enables phase shifted measurements while providing a stationary coherence envelope localized at the target OPD condition over a full aperture field of view.

In Figure 7 and Figure 8, the ability to reconfigure the OPD condition between two difference locations is demonstrated.

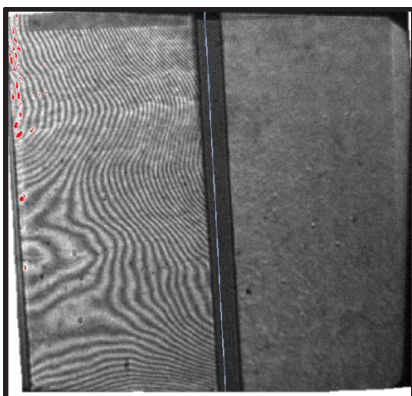


FIGURE 7. Experimental results showing high coherence fringes localized at OPD = 50.8 micron air gap and no interference fringes at OPD = 127 micron air gap.

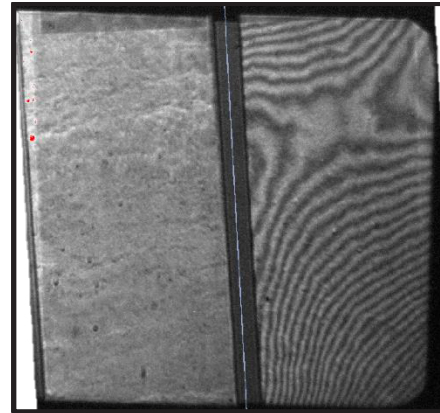


FIGURE 8. Experimental results showing no interference fringes at OPD = 50.8 micron air gap and high coherence fringes localized at OPD = 127 micron air gap.

Four phase shifted interferogram frames are captured in each of these conditions and the surface is analyzed in Reveal™. Each surface profile is displayed in Figure 9 and Figure 10.

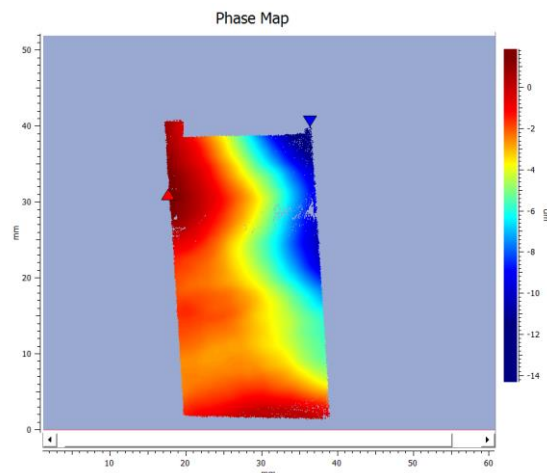


FIGURE 9. Measured phase map (units of microns) of OPD = 50.8 micron air gap.

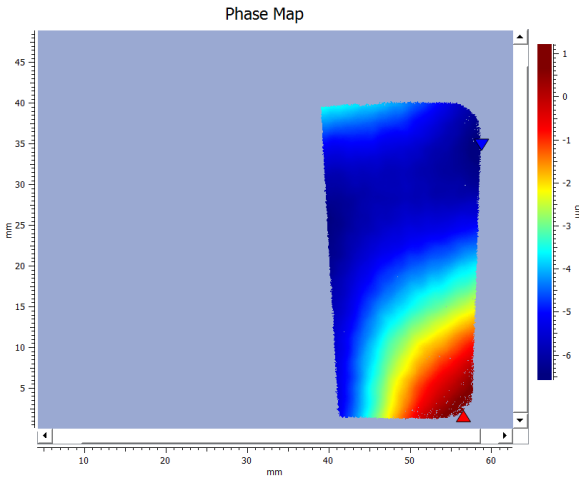


FIGURE 10. Measured phase map (units of microns) of OPD = 127 micron air gap.

The same surfaces with Tilt, Power, and Astigmatism terms removed are displayed in Figure 11 and Figure 12.

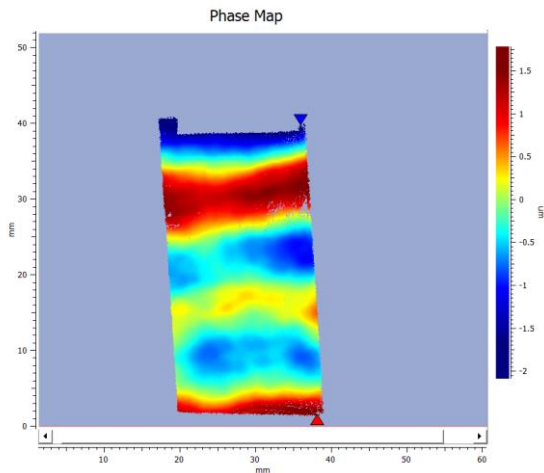


FIGURE 11. Measured phase map with Tilt, Power, and Astigmatism terms removed for OPD = 50.8 micron air gap.

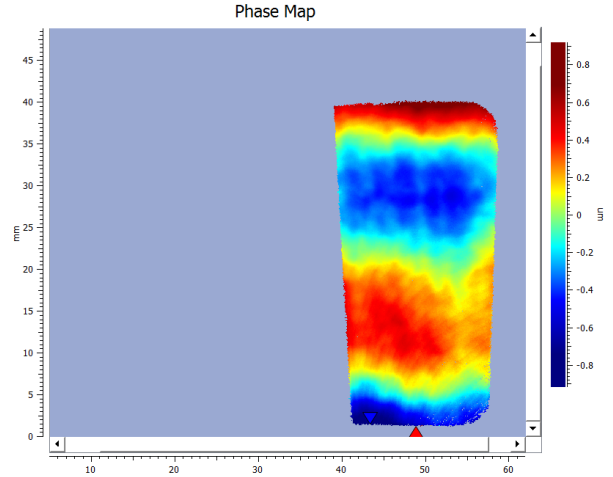


FIGURE 12. Measured phase map with Tilt, Power, and Astigmatism terms removed for OPD = 127 micron air gap.

LIMITATIONS

While this approach provides a solution to surface isolation and the measurement of very thin optical windows, there are few associated limitations and challenges, as well.

First, the measurement of a surface with at a designated fringe location is predicated on the fringe location exceeding the nominal coherence envelope. That is to say that the desired measurement distance exceeds the coherence length of the non-modulated source

bandwidth, $l_c = \frac{\lambda_0^2}{2\Delta\lambda}$, which for practical broadband sources is on the order of 20 microns. Additionally, the maximum achievable fringe distance is reached when the spectral modulation is equivalent to the spectral bandwidth. This problem is easily mitigated, however, by using a larger spectral bandwidth source.

Furthermore, an inherent limitation of this approach is the maximum achievable contrast of interference fringes is limited to, $|\gamma| = 0.5$. However, with 10-bit and 12-bit cameras becoming the industry standard the increased digitization depth more than compensates any contrast loss.

The method presented here is not limited to finite phase shifting, nor mechanical calibration. Thus this method consistently produces virtually endless phase shifting with high linearity which can be used with N-frame phase unwrapping algorithms which decrease the noise floor and provide higher repeatability.

CONCLUSION

Only a few interferometric technologies exist that address the problem of multiple surface reflection in conventional interferometry. The current methods all rely, in some form or another, on the mechanical motion and alignment of a secondary, path-matching cavity. Spectrally Controlled Interferometry provides a new approach that can achieve these measurements without the need for mechanical alignment. Through the control of the spectral width, spectral modulation frequency, and the phase of the modulation function, high contrast, localized fringes can be produced and phase shifted for a variety of measurement applications including thin optical window measurement and multiple surface isolation and scanning without reconfiguring system alignment.

Future work includes demonstrating this approach in a variety of novel measurement applications and exploring the noise floor and accuracy of this approach.

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IMPROVEMENT OF THE ANGULAR PRECISION OF AN OPTICAL METROLOGY PLATFORM BY DISTURBANCE COMPENSATION

Thomas Riel^{†,‡}, Andras Galffy[†], Andreas Sinn, Georg Schitter
Automation and Control Institute (ACIN)
TU Wien, Vienna, Austria

INTRODUCTION

Precise angular motion is required in a number of applications, such as precision robotics [1] and optical communication systems [2]. For example, optical communication systems rely on small beam divergence to increase efficiency and signal to noise ratio (SNR). As the intensity of the laser beam is concentrated in the center, the required precision of the tracking system is usually one order of magnitude lower than the beam divergence. For typical beam divergences, which are in the μrad range, this results in a required tracking system precision in the sub- μrad range [3, 4]. This precision has to be maintained over a wide actuation range, of several tenth of degrees, and also for high velocity motions of up to several degrees per second. One realization of such a precise angular motion system over a wide actuation range uses direct-drive permanent magnet synchronous motors (PMSMs). Direct-drive systems achieve high precision by reducing unwanted effects of gear boxes, such as backlash and friction. However, to achieve a precise motion with errors in the sub- μrad range, a detailed analysis of the present disturbance sources and a good mechatronic system design are indispensable [5].

The design of an optical metrology platform, used for laser ranging and tracking, is described in literature [6]. The achieved RMS tracking error is $1.4 \mu\text{rad}$ for maximal velocities of up to 1 dps, but approaches $8 \mu\text{rad}$ for higher velocities of up to 10 dps. The described system uses direct-drive motors with cascaded position and velocity control. The used controllers are of proportional-integral type and are supplemented by feed-forward control. The pose of the system is measured by optical encoders which have a resolution of less than 100 nrad . Although the implemented control concept is widely used in industrial applications,

it does not utilize the entire potential of the described system.

In this paper, the system under investigation is an optical metrology platform used for precision laser ranging and tracking. The platform can be positioned by direct-drive PMSMs in two rotational axes with high precision. The pose of the system is detected with optical encoders. The mechatronic system design of the investigated system is optimized to perform high precision motions [7]. In recent work it was shown that for motions of the optical metrology platform at high angular velocity (up to 20 dps), torque disturbances are the dominating error source [8]. One solution for mitigating these unwanted effects without redesign of the hardware is by utilizing advanced control methods [9].

This paper presents the utilization of such control concepts to improve the achievable angular precision of the investigated platform. A disturbance observer in combination with feed-forward control is used to reduce the detrimental effect of the disturbance forces on the system precision. The observer estimates the pose as well as the disturbance forces based on the measured input currents of the direct-drive PMSMs and the encoder output. In addition to state feedback control, the estimated disturbance force is then used for feed-forward control to cancel the effect of these disturbances. Using the proposed control structure, not only friction forces but also cogging moments and gravitational forces are compensated. To characterize the motion of the complete system, which also includes an optical system, an external measurement system is used. The achieved system performance is compared to pure feedback control, verifying the improvement in angular precision.

SYSTEM DESCRIPTION

The optical metrology platform, as shown in Figure 1 (b), is used to position an optical measurement system with high precision. The platform incorporates two direct-drive PMSMs with eleven

[†] Both authors contributed equally.

[‡] Corresponding author: riel@acin.tuwien.ac.at

pole pairs and a rated torque of 6 N m and 14 N m, respectively. The pose of the platform is measured by one optical encoder per axis. The used encoders have a noise floor of 160 nrad. The platform should be able to move at velocities of up to 10 dps, while retaining high precision with an servo error in the sub- μ rad range.

SYSTEM MODELING

For controller design, an accurate model of the individual rotational axis is needed. For modeling, the system is split into an electrical and mechanical subsystem [10].

The mechanical system is described as a lumped mass model

$$\frac{d\varphi}{dt} = \omega \quad (1)$$

$$\frac{d\omega}{dt} = \frac{1}{J} (T_e - T_f - T_d), \quad (2)$$

where φ is the angular position, ω the angular velocity, J the moment of inertia, T_e the electrical torque, T_f the torque due to the friction force F_f and T_d models an external load or disturbance torque.

The frictional torque T_f can be modeled using a simple stationary friction model [11]

$$T_f = \left(T_C + (T_S - T_C) e^{-\frac{\omega}{\omega_0}} \right) \text{sgn}(\omega) + \beta \omega, \quad (3)$$

where T_C is the Coulomb friction, T_S is the static friction, ω_0 the characteristic angular velocity corresponding to the Stribeck effect, and β the coefficient of viscous friction. Unfortunately friction varies significantly with position, temperature, humidity, orientation of the mount, etc. Extending the stationary friction model (3) with a dynamic model, e.g. a LuGre Model [12], still doesn't take into account these dependencies. Thus in this paper an observer is used to estimate the effect of the friction and disturbances torque in real time. By further compensating the estimated friction and disturbances an exactly linear plant behavior is obtained.

To describe the electrical subsystem, an electrical model of the PMSM is used [13]. To simplify the electrical system, all quantities are transformed into the rotor coordinate system by means of the power invariant Park transformation [10, 14, 15].

The resulting electrical system in rotor coordinates is described by

$$\frac{di_d}{dt} = \frac{1}{L_d} (u_d - R i_d + \omega_e L_q i_q) \quad (4)$$

$$\frac{di_q}{dt} = \frac{1}{L_q} (u_q - R i_q + \omega_e L_d i_d - \omega_e \Psi_M), \quad (5)$$

where L_d, L_q are the d, q axis inductances, u_d, u_q the d, q axis stator voltages, R the stator resistance, i_d, i_q the d, q axis stator currents, Ψ_M the mutual magnetic flux due to the rotor magnets, Z_p the number of pole pairs and $\omega_e = Z_p \omega$ the electrical velocity.

The link between the electrical and mechanical system is given by the generated electrical torque given by

$$T_e = \frac{3}{2} Z_p (\Psi_M i_q + (L_d - L_q) i_d i_q). \quad (6)$$

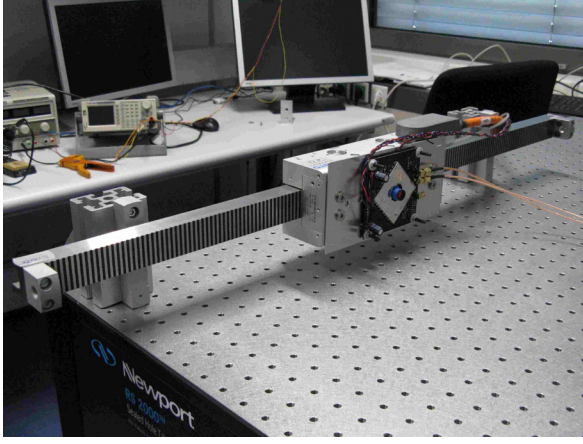
DISTURBANCE OBSERVER

For the design of the disturbance observer it is assumed that the bandwidth of the current controller is high enough to be neglected for design of the motion control system. As the typical motion control bandwidth for the investigated system is in the 10 Hz range while the current control bandwidth is in the 1 kHz range, this assumption is valid. Therefore, by assuming that the applied current is equal to the desired current, $i_q = i_q^{des}$ and $i_d = i_d^{des} = 0$, the electromechanical model of the PMSM, (1)-(6), can be reduced to

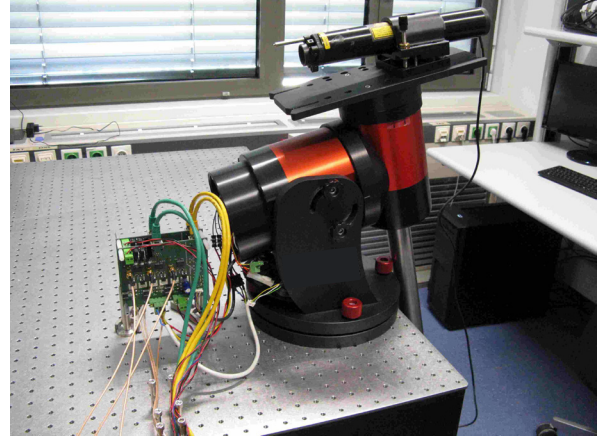
$$\frac{d\varphi}{dt} = \omega \quad (7a)$$

$$\frac{d\omega}{dt} = \frac{1}{J} (k_M i_q^{des} - T_f - T_d), \quad (7b)$$

where $k_M = \frac{3}{2} Z_p \Psi_M$ is the motor constant of the PMSM, T_f the torque caused by friction, and T_d additional torque caused by external disturbances or loads, such as gravitational forces, floor vibrations or aerodynamic forces. As the disturbance torque and friction torque are either unknown or difficult to model, they are estimated by means of a Luenberg-Observer. To achieve a high precision estimation, only the most reliable system parameter are used for the observer. As both the phase currents of the PMSM and the mechanical position of the axis are measured with high precision, these values are used as inputs to the observer. To reduce the model uncertainty of the system model (7), only fundamental kinematics



(a)



(b)

FIGURE 1. The used experimental setup consisting of (a) external measurement system and (b) optical metrology platform with mounted HeNe laser.

are used to describe the system: The angular velocity ω is the derivative of φ , and the derivative of ω is the angular acceleration α caused by T_e , T_f and T_d .

The angular acceleration caused by T_e is given by $\alpha_M = \frac{k_M}{J} i_q^{des}$, where i_q^{des} is the desired current. The remaining parameters are the inertia J and the motor constant k_M , which can be summarized to one motor coefficient c_M . Thus, α_M can be written as $\alpha_M = c_M i_q^{des}$. For the observer c_M can either be calculated from well known system parameters or identified by simple experiments.

The angular acceleration caused by α_f can only be modeled with high uncertainty and is affected by position, humidity, temperature, time, etc. Furthermore, uncertainties in ω , which can only be estimated, highly affect friction models like given in Equation (3). Also α_d is highly uncertain as it is being caused by unknown external disturbances and loads. For not being able to separate the effects of α_f and α_d they are combined to one load acceleration α_l . Assuming α_l changes slowly enough to be considered constant, the corresponding disturbance model is $\frac{d\alpha_l}{dt} = 0$.

Based on this model a Luenberg-Oberserver is designed, estimating $\hat{\varphi}$, $\hat{\omega}$, and $\hat{\alpha}_l$ for a constant,

beforehand identified parameter $\hat{c}_M$:

$$\frac{d\hat{\varphi}}{dt} = \hat{\omega} + k_{1,o}(\varphi - \hat{\varphi}) \quad (8a)$$

$$\frac{d\hat{\omega}}{dt} = \hat{c}_M i_q^{des} + \hat{\alpha}_l + k_{2,o}(\varphi - \hat{\varphi}) \quad (8b)$$

$$\frac{d\hat{\alpha}_l}{dt} = k_{3,o}(\varphi - \hat{\varphi}) + e_{\varphi,I} \quad (8c)$$

$$\frac{de_{\varphi,I}}{dt} = k_{I,o}(\varphi - \hat{\varphi}). \quad (8d)$$

An additional state $e_{\varphi,I}$ is introduced to provide integral action for the error of the position estimation $e_{\varphi} = \varphi - \hat{\varphi}$.

The dynamics of the observer errors e_{φ} , e_{ω} , e_{α_l} , and $e_{\varphi,I}$ according to (8) are given by

$$\frac{de_{\varphi}}{dt} = \frac{d\varphi}{dt} - \frac{d\hat{\varphi}}{dt} = (\omega - \hat{\omega}) - k_{1,o} e_{\varphi} \quad (9a)$$

$$\begin{aligned} \frac{de_{\omega}}{dt} = \frac{d\omega}{dt} - \frac{d\hat{\omega}}{dt} = (c_M - \hat{c}_M) i_q^{des} \\ + (\alpha_l - \hat{\alpha}_l) - k_{2,o} e_{\varphi} \end{aligned} \quad (9b)$$

$$\frac{de_{\alpha_l}}{dt} = \frac{d\alpha_l}{dt} - \frac{d\hat{\alpha}_l}{dt} = -k_{3,o} e_{\varphi} + e_{\varphi,I} \quad (9c)$$

$$\frac{de_{\varphi,I}}{dt} = -k_{I,o}(\varphi - \hat{\varphi}). \quad (9d)$$

Substituting $e_{\omega} = \omega - \hat{\omega}$, $e_{\alpha_l} = \alpha_l - \hat{\alpha}_l$, and $\Delta_{c_M} = c_M - \hat{c}_M$ in Equation (10) and writing it in matrix

representation results in

$$\frac{d}{dt} \begin{bmatrix} e_{\varphi,I} \\ e_{\alpha_I} \\ e_{\omega} \\ e_{\varphi} \end{bmatrix} = \begin{bmatrix} 0 & 0 & 0 & -k_{I,o} \\ 1 & 0 & 0 & -k_{3,o} \\ 0 & 1 & 0 & -k_{2,o} \\ 0 & 0 & 1 & -k_{1,o} \end{bmatrix} \begin{bmatrix} e_{\varphi,I} \\ e_{\alpha_I} \\ e_{\omega} \\ e_{\varphi} \end{bmatrix} + \begin{bmatrix} 0 \\ 0 \\ \Delta c_M \\ 0 \end{bmatrix} i_q^{des}. \quad (10)$$

For $\Delta c_M = 0$, corresponding to perfect knowledge of the acceleration caused by the input currents of the PMSM, the observer error decays to $\vec{0}$ according to the characteristic polynomial $p_o(s) = s^4 + k_{1,o}s^3 + k_{2,o}s^2 + k_{3,o}s + k_{I,o}$. By using the MATLAB command `poly`, the observer gains are calculated based on the desired pole locations.

The estimated disturbance acceleration $\hat{\alpha}_I$ is then used in the control of the PMSM as additional control input

$$i_q^{des} = \frac{u - \hat{\alpha}_I}{\hat{c}_M}, \quad (11)$$

where u is the output of the linear motion controller.

By substituting (11) in (7), the system dynamics can be rewritten with the new input u to be

$$\frac{d\varphi}{dt} = \omega \quad (12a)$$

$$\frac{d\omega}{dt} = \alpha_I - \frac{c_M}{\hat{c}_M} \hat{\alpha}_I + \frac{c_M}{\hat{c}_M} u. \quad (12b)$$

Assuming a good identification of the motor coefficient c_M , i.e. $\hat{c}_M \approx c_M$, and therefore also $\alpha_I \approx \hat{\alpha}_I$, results in an exactly linear behavior of the plant

$$\frac{d^2\varphi}{dt^2} = \frac{d\omega}{dt} = u. \quad (13)$$

By compensating the disturbance acceleration α_I with its estimated value $\hat{\alpha}_I$, not only friction forces but also cogging moments and gravitational forces are compensated.

STATE FEEDBACK

The resulting plant dynamics (12) are then controlled by means of a state feedback controller with integral action

$$u = \alpha^{des} + k_{2,r}(\omega^{des} - \hat{\omega}) + k_{1,r}(\varphi^{des} - \hat{\varphi}) + k_{I,r}e_{\varphi,I,r}. \quad (14)$$

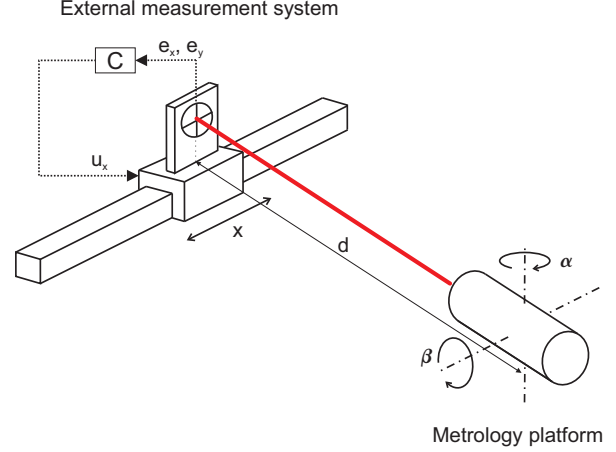


FIGURE 2. Schematic representation of the external optical measurement system. The optical metrology platform can be positioned in two rotational axes (α, β). Its motion is observed by an external optical measurement system.

The resulting error dynamics of the system (12) with state feedback controller (14) in matrix form are given by

$$\frac{d}{dt} \begin{bmatrix} e_{\varphi,I,r} \\ e_{\varphi} \\ e_{\omega} \end{bmatrix} = \begin{bmatrix} 0 & 1 & 0 \\ 0 & 0 & 1 \\ -k_{I,r} & -k_{2,r} & -k_{1,r} \end{bmatrix} \begin{bmatrix} e_{\varphi,I,r} \\ e_{\varphi} \\ e_{\omega} \end{bmatrix}. \quad (15)$$

The error decays to $\vec{0}$ according to the characteristic polynomial $p_r(s) = s^3 + k_{1,r}s^2 + k_{2,r}s + k_{I,r}$. By using the MATLAB command `poly`, the controller gains are calculated based on the desired pole locations. The poles of the controller are placed at a frequency which is a factor ten below the observer bandwidth.

EXPERIMENTAL SETUP

To verify the proposed control concept, experimental measurements are performed. For this purpose, the proposed disturbance observer and state feedback controller are implemented on a dSpace Rapid Prototyping System (dSPACE GmbH, Paderborn, Germany). Custom made switched mode power amplifiers are used to drive the PMSMs with high precision [16]. The optical metrology platform is mounted on an optical table and loaded with a HeNe laser as the optical metrology system, as shown in Figure 1 (b).

To investigate the motion of the metrology platform, the internal optical encoders of the axes are used. In addition, an external measurement sys-

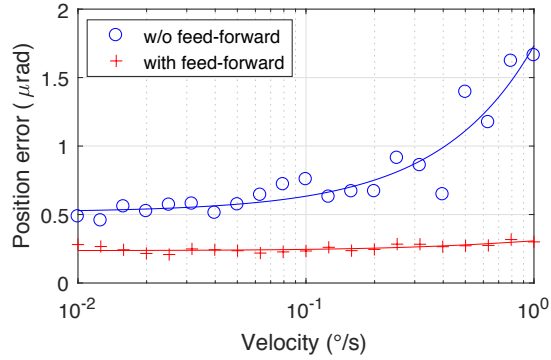


FIGURE 3. Comparison between the measured servo error with and without disturbance feed-forward.

tem is used to verify the motion of the system, as shown in Figure 1 (a).

The external measurement system is comprised of a linear motor with magnetic encoder and a 4-quadrant detector (4QD), as shown in Figure 2. The error signal of the 4QD is utilized to position the linear motor, thereby achieving a wide measurement range. The pose of the optical metrology platform results from the combination of the 4QD error signals and the measured position of the linear motor. Using this measurement system, the complete motion of the metrology platform, including an otherwise undetectable motion of the optical system, can be detected.

EXPERIMENTAL RESULTS

The performance of the metrology platform is compared with and without disturbance feed-forward. For this purpose the platform is moved with constant velocity. The bandwidth of the state feedback is kept constant at 15 Hz. The velocity is then incremented in several steps from 0.01 dps to 1 dps and the servo error is recorded. The results are shown in Figure 3.

For the case without disturbance feed-forward, the RMS servo error ranges from 480 nrad for 0.01 dps to 1.6 μ rad for 1 dps. In the case with disturbance feed-forward, the RMS servo error ranges from 280 nrad for 0.01 dps to 300 nrad for 1 dps. With the disturbance feed-forward enabled, the servo error is only weakly dependent on the angular velocity. This is also visible in Figure 4, where the servo error with and without disturbance feed-forward is plotted over time for a velocity of 1 dps. When disturbance feed-forward is enabled, the RMS position error is reduced from 1.6 μ rad to

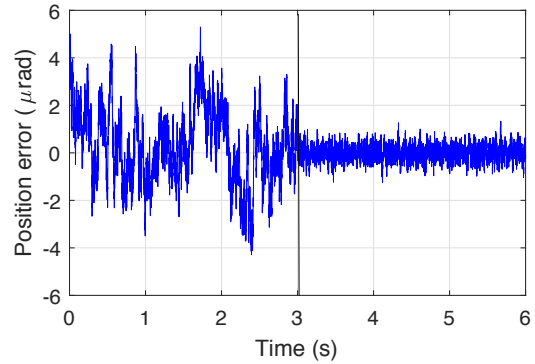


FIGURE 4. Position error of the metrology platform for a velocity of 1 dps. After the disturbance observer is enabled at 3 s, the RMS position error is reduced from 1.6 μ rad to 300 nrad.

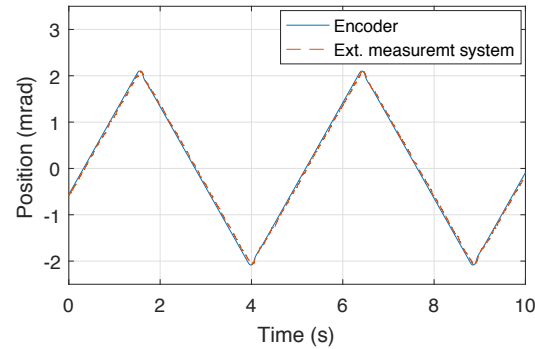


FIGURE 5. Comparison between the measured encoder position and the measurement of the external measurement system.

300 nrad, which is an improvement by a factor of more than 5.

To verify the motion of the optical metrology system, the external measurement system is used. The comparison between the internal encoder and the external measurement system for a triangular scanning motion with 0.1 dps is shown in Figure 5. The overall motion of the optical metrology system is well represented by the measurement of the internal encoders, showing no additional dynamics.

CONCLUSION

This paper demonstrates the utilization of disturbance feed-forward control for high precision motion control of a optical metrology platform. The proposed disturbance observer is designed purely based on fundamental kinematic relations,

using only precisely measurable signals of the PMSM. The implementation of the proposed control concept results in a drastic improvement of the performance of the metrology platform. With the disturbance feed-forward enabled, the RMS position error is essentially independent of angular velocity. Compared to feedback control without disturbance feed-forward, a factor of 5 improvement of the RMS position error is achieved.

ACKNOWLEDGEMENTS

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Scalability in Contact Lens Manufacturing to Improve the Refractive Performance and User Comfort

Jonathan D. Ellis^{a,b}, Daniel R. Brooks^b, Sam C. Butler^{a,c}, Len A. Zheleznyak^{b,c},
Gustavo A. Gandara-Montano^b, Kaitlin T. Wozniak^b, Wayne H. Knox^b

^aDepartment of Mechanical Engineering, University of Rochester, Rochester, NY 14627

^bThe Institute of Optics, University of Rochester, Rochester, NY 14627

^cClerio Vision, Inc., Rochester, NY 14618

INTRODUCTION

Contact lenses are traditionally manufactured using a multi-step molding process. Diamond-turned master molds are first used to generate injection molded plastic lens blanks. These lens blanks serve as both the final mold for the hydrogel contact lens and the lens carrier through the manufacturing process chain. Contact lenses are traditionally manufactured in 0.25 D defocus power and cylindrical power increments. Additionally, contact lenses are designed not only for their prescriptive refractive correction value, but also based on their “feel” and comfort for the user. The balance and comfort of the contact lens on the user’s eye requires weighting of the contact lens and a specifically designed edge to hold and reposition the contact lens when the user blinks, even for non-astigmatic designs. Figure 1 shows the thickness variation of two contact lenses, one with side ballasts and the other with a ballast below the central optical zone.

These ballast are required for astigmatic contact lenses to prevent rotation of the optical axis *and* they are also needed for spherical contact lenses for user comfort [1]. Thus, astigmatic contact lenses need different optical designs to account for not only the refractive correction but also the balance on the eye. Astigmatic corrections are usually limited to correction angles at every 10° because the number of permutations between defocus power, cylindrical power, and astigmatic axis grows considerably, as depicted in Figure 2. For example, if the manufacturing space for ~95% of patients is considered, the refractive correction needed is less than 3 D. In that space, there are 25 different defocus increments (-3 D to 3 D), 25 different astigmatic power increments (-3 D to 3 D) at 36 different astigmatic angles (0° to 350°). This translates to over 22,000 individually designed, manufactured, and qualified contact lens molds, where the weight and

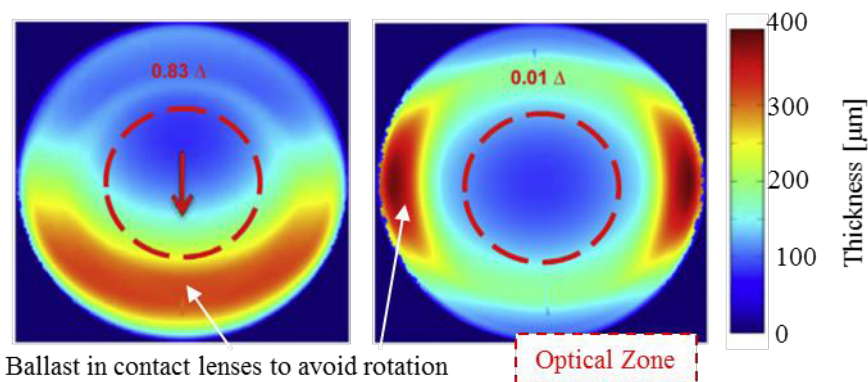


FIGURE 1. Thickness measurements of two traditional contact lenses with ballast at the bottom (left) and ballast on the sides (right) to prevent rotation. The ballast is necessary for astigmatic corrections but also critical for user comfort. Figure adapted from [1].

“feel” of the contact lens differs. Also, the magnitude of refractive corrections can be $>10+ D$, leading to an increasingly complex production line.

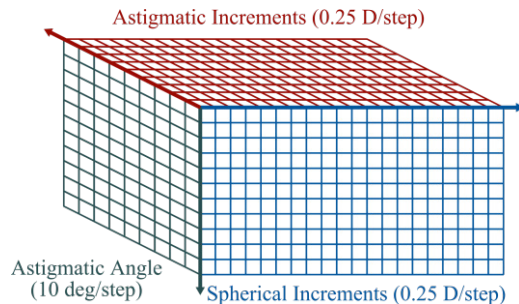


FIGURE 2. Design space for contact lenses with varying defocus, astigmatic power, and astigmatic angle. Between $-3 D$ and $+3 D$, the design space requires over 22,000 different contact lens molds and shapes.

Thus, each different refractive correction for a contact lens line requires an individually designed and manufactured diamond-turned mold, and the multi-step process from metal master mold to plastic injection molded blank to hydrogel contact lens leads to different tolerance stacking as a function of lens thickness, reducing the overall achievable precision.

IRIS: Intra-tissue Refractive Index Shaping

Laser-based processing methods offer the potential to provide scalability while at the same time reducing the complexity and inventory requirements for contact lens manufacturing. Our process, IRIS or Intra-Tissue Refractive Index Shaping, is a scalable manufacturing process where a femtosecond laser can be used to photo-modify the refractive index of an ophthalmic material, both hydrogels for contact lenses and native cornea [2-6].

In hydrogels, we scan the focal volume using 405 nm, ~ 160 fs pulse light with an 80 MHz repetition that is frequency doubled from an 810 nm Ti:Sapphire source. By tailoring the focal spot properties and optical power delivered, we can locally change the refractive index of the hydrogel contact lens

to generate a gradient index (GRIN) refractive devices, including arbitrary refractive corrections [7]. Because we can induce refractive power without changing the shape of the contact lens, we can greatly simplify the molding and manufacturing process for contact lenses and reduce the number of required permutations to account for user comfort. We envision a base series of molds, on the order of 10s of molds to generate a baseline refractive power that is then tailored to the desired refractive correction using an IRIS layer in the hydrogel, as depicted in Figure 3.

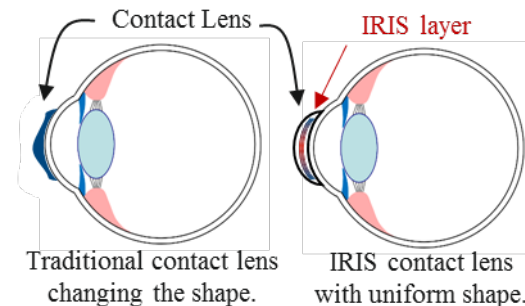


FIGURE 3. (left) Traditional contact lens using the shape of the optical surface to introduce optical power. (right) An IRIS contact lens with a base contact lens molded for user comfort and feel with an IRIS layer for inducing the necessary refractive correction.

IRIS WRITING & QUALIFICATION

Currently, we use a 20x, NA 1.05 water immersion objective to focus the light to the sample. The microscope objective is mounted on a 3-axis inverted linear stack stage for global positioning over a 60 mm by 60 mm by 30 mm volume. Light is relayed from a 2-axis galvanometer, which is synchronized to the X and Y motions of the stage stack, as shown in Figure 4. The microscope’s field of view is 720 μm at the full NA and the galvanometer and stage motions combine to enable high speed scanning over much larger fields. Based on the current optical properties and the galvanometer scanning, focal spot velocities

can be generated up to 900 mm/s or more if a different microscope objective is used.

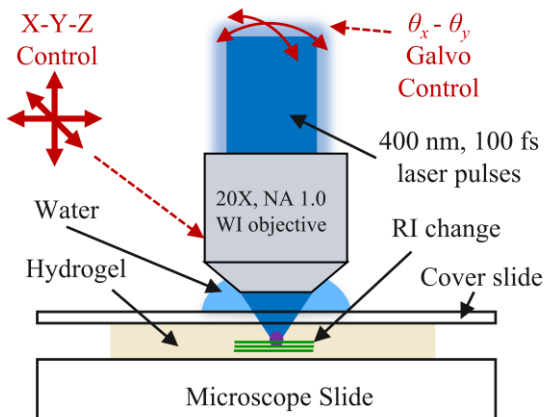


FIGURE 4. Schematic of the IRIS writing system with 5 axes of motion control to allow high NA writing over 60 mm by 60 mm by 30 mm volumes.

The first step in generating refractive devices is understanding how the material behaves as a function of speed, incident laser power, and other system parameters. Figure 5 shows an interferogram of 21 calibration “bars” written in Contamac58 hydrogel at a speed of 300 mm/s. The 7x3 matrix of bars has a uniformly increasing laser power. Custom software was written to analyze these carrier fringes to measure the phase induced by IRIS. For this calibration run, the third column shows the power too high and the sample burning. However, the top power in the middle column (1,2) shows 1 wave of phase change at 633 nm. Generating 1 wave of phase change is the critical threshold as it allows for any refractive device to be generated via Fresnel lens designs.

Using different system parameters such as speed, power, and NA can significantly impact IRIS writing. In Figure 5B, the points depict the highest measurable phase change prior to burning in the sample. For some combinations, IRIS can generate over 2 waves of phase change at 555 nm, which is the most sensitive wavelength for the human visual spectrum.

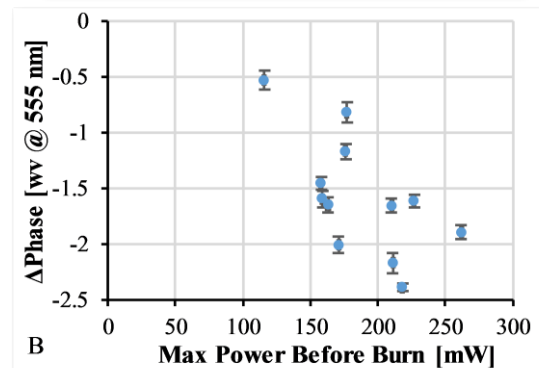
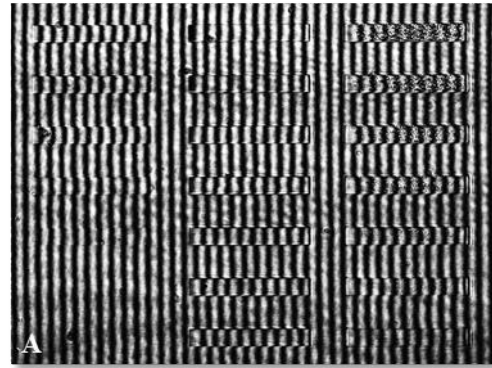


FIGURE 5. (A) Interferogram of 7 rows and 3 columns of IRIS calibration bars written with increasing power. The last column demonstrates burning in the sample. (B) A series of maximum phase change from 11 different experiments with differing system parameters.

Once the material’s response is known to IRIS, the refractive index can be altered sufficiently to generate a refractive device. Using this writing system, 9 stripes of 720 μm by 6.5 mm can be stitched together in a Fresnel structure build a refractive device. Figure 6 shows a qualitative example of a refractive device written by IRIS. In Figure 6B, the lens is shifted over the optical table hole, demagnifying the image of the hole, demonstrating that negative power is induced by the -1 D lens.

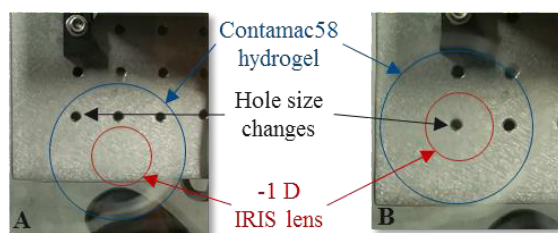


FIGURE 6. (A) A 6.5 mm IRIS lens written in Contamac58 with an intended refractive change of -1 D. When the lens is placed over the optical table whole (moving from A to B), the size of the whole changes, indicating refractive power.

QUANTITATIVE ASSESSMENT

There are several metrology tools that can be used to quantify the performance of IRIS refractive devices. Metrology tools include wavefront metrology, interferometry, MTF testing, and vision simulation. Figure 7 shows the measured transmitted wavefront of the IRIS refractive device with the background removed. While a Fresnel lens was written, the measured wavefront change is smooth and uniformly changing. The measured wavefront was +1.63 D, which is close to the intended +1.65 D intended change. Additionally, considering contact lenses are currently manufactured in 0.25 D increments, this lens exhibits a ~10x improvement in that resolution limit.

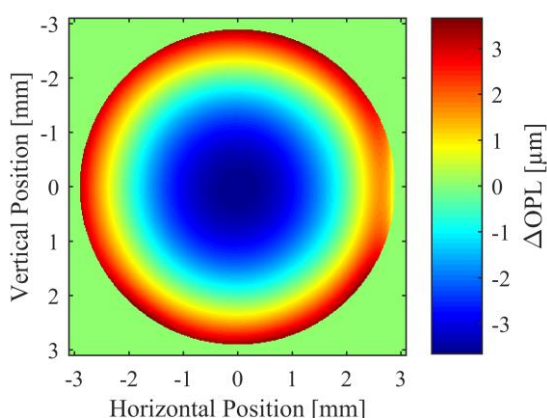


FIGURE 7. Transmitted wavefront of a Contamac58 IRIS refractive device with an intended +1.65 D intended spherical power generated a measured +1.63 D of change.

A custom MTF bench was designed and built to measure the performance of refractive devices written by IRIS. Figure 8 shows an image of an Air Force Bar Chart viewed through the lens measured at 545 nm, which is close to the highest response of the human visual spectrum. The highlighted box indicates that 40 line pairs per degree are readily resolvable, even though typical contact lenses are designs for only up to 30 lp/degree.

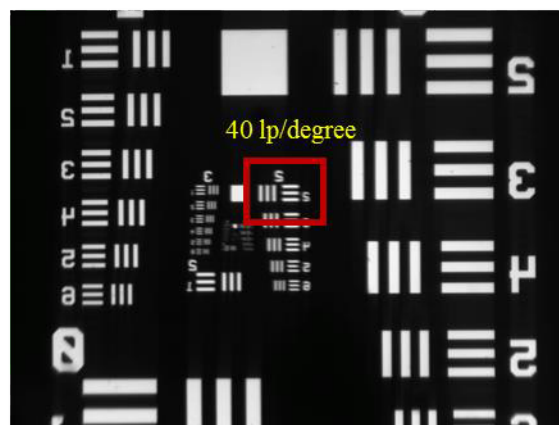


FIGURE 8. Air Force Bar Chart viewed through the IRIS +1.63 D refractive device at the best focus. This is measured at the near center of the human visual spectrum where the sensitivity for humans is the highest.

Figure 9 shows the measured MTF of the +1.63 D IRIS refractive device compared with the baseline response of the instrument and the human visual response limit. The human visual response limit is 60 cycles/degree although visual refractive correction typically only assess up to half that value. The ratio of the area below the MTF radial average curve up to 60 cycles/degree as compared with the instrument baseline is >90%. The measured Strehl ratio is about 71%, which is close to the 80% limit where the Strehl ratio is considered diffraction limited.

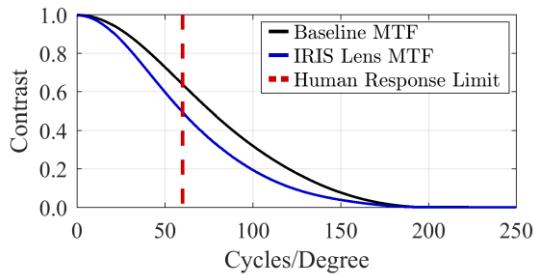


FIGURE 9. Measured radial average MTF for the +1.63 D IRIS refractive device as compared to the instrument baseline and limit of the human visual response.

CONCLUSIONS

IRIS is a process that can generate refractive change in ophthalmic materials. The amount of refractive change can produce over 1 wave of phase change at 555 nm, which is the peak sensitivity of the human visual spectrum. By specifically structuring the refractive change, 9 stripes of IRIS can be stitched to generate clinically relevant refractive devices. The measured performance of a +1.65 D device over a 6.5 mm optical zone shows less than 2% error in the induced refractive change from the intended change. Moreover, the MTF measurements show a near diffraction limited performance of the lens.

In the future, we look to generate arbitrary refractive devices for astigmatic correction and to assess the visual performance of these lenses through an adaptive optics simulator.

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The History and Future of the Stedman Diagram

Alexander Blum¹, Chris Evans¹

¹Center for Precision Metrology
University of North Carolina at Charlotte
Charlotte, North Carolina, United States

ABSTRACT

The original Stedman diagram, first published in 1987, provides a representation of the application range of a given measurement instrument in amplitude/spatial wavelength space with closed boundaries. The boundaries form a box defined by resolution and range in the horizontal and vertical dimensions, with additional constraining lines imposed by slope and curvature limits. This presentation reviews the history of the Stedman diagram, discusses its limitations as a method of instrument evaluation, and explores ways to expand the diagram by accounting for metrological characteristics of the instrument. These ideas are meant to stimulate discussion of methods to effectively communicate technical information between optical designers and manufacturing engineers.

INTRODUCTION

The basic concept dates to the late 1970s when Stedman was working at the UK National Physical Laboratory with Albert Franks, whose main interest was grazing incidence X-ray optics (Stedman M, personal communication with CE, 2017). An early version of the diagram appears in a 1981 paper by Stedman on metrology of grazing incidence X-ray optics [3, 4]. In 1979 Church used a similar representation to show the ranges of roughness amplitudes and wavelengths of roughness relevant to the performance of optics for HeNe and CO₂ laser systems [5].

The first part of this presentation briefly reviews the original diagram's construction and application to surface measurement. The original diagram's limitations are also discussed. Evolution of the Stedman diagram has been slow, although the 1987 paper is widely cited. Thomas plotted multiple instruments spanning a large range on a single diagram in 1998, using this as a tool to analyze limitations in topological measurement technology [6]. Since then, others have expanded on the original diagram. Rosen et al added measurement time as a third axis,

providing an additional parameter for instrument evaluation [2]. Jones and Leach evaluated in-plane scanning speed for a contact profilometer, using this criteria to impose additional limitations on the diagram, such as maximum and minimum probe forces [7]. These efforts are reviewed in the second part of this presentation.

Any attempt to expand the original diagram has a potential downside – adding complexity to the original diagram is likely to increase the difficulty of extracting quantitative information about instrument performance, especially when presented in a non-interactive format. Another limitation is that as the diagram is currently drawn the information encoded is binary; the surface topography of interest either falls within the lines or outside them. In reality, the metrological characteristics of individual instruments lead to variations in instrument performance that create 'fuzzy' boundaries. Instrument metrological performance and uncertainty in the result of a particular measurement may vary significantly within the "usable" area. The final part of this presentation presents methods developed to capture nuances of instrument performance in hopes of stimulating discussion of the future of the Stedman diagram.

THE ORIGINAL STEDMAN DIAGRAM

The original Stedman diagram was developed as method to evaluate the performance of a surface-measurement instruments by plotting the limits of its ability to measure sinusoids in two-dimensional log-amplitude log-wavelength (AW) space. The sine function was chosen for both mathematical convenience and existing popularity as a method for modeling surfaces. Nearly any surface profile can be resolved into sinusoidal components through Fourier analysis, exceptions are discussed below. This means the Stedman diagram fundamentally 'exists' in the Fourier domain, allowing the diagram to encode information about the roughness, waviness, and form of a surface that can be captured by a specific measurement.

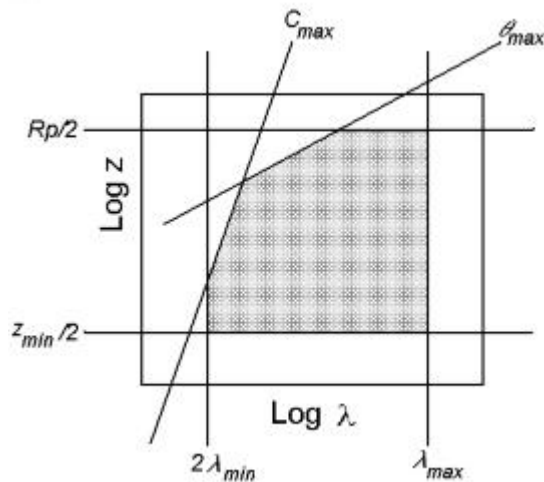


FIGURE 1. *Generic Stedman diagram, from Rosén [2]*

Slope and curvature limits are found by taking the first and second derivative of the sinusoidal profile $z = \sin(2\pi x/\lambda)$ and finding the maxima of each. Resolution and amplitude limits form a box on the diagram, while slope and curvature limits impose constraints defined by lines with slopes of 1 and 2, respectively. The final result is the diagram shown in Figure 1, outside which the instrument cannot measure.

EVOLUTION OF THE STEDMAN DIAGRAM

The first scholarly critique of the Stedman diagram was that measurement time, which is important when choosing instruments for a production environment, is ignored [8]. Rosen et al added measurement time to the diagram as a third axis, theoretically allowing quantitative information about acquisition time, which is commonly excluded from surface measurement device specifications [9], to be extracted from the diagram.

The diagram can also be used as a tool to evaluate the cutting edge in surface measurement. Thomas presents a Stedman diagram that summarizes the coverage of topographic measurement techniques in a 1998 paper, with stylus, optical, and AFM measurement instruments presented as one measurement envelope. This envelope covers amplitudes from one nanometer to several millimeters at wavelengths ranging from sub-micron to kilometer scale. Electron microscopy extends coverage to even shorter wavelengths, but extracting surface height data from these measurements is difficult [10].

Coordinate measuring machines represent the upper end of what might be considered “topography” and the longest wavelengths that

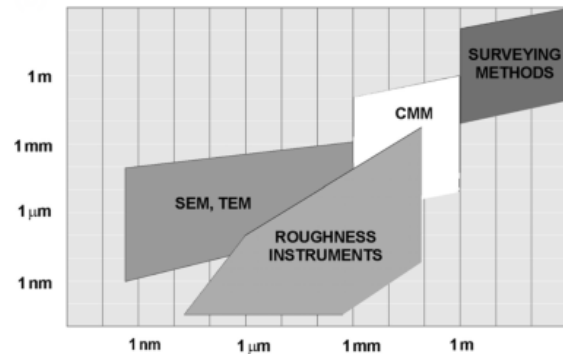


FIGURE 2. *Global Stedman diagram, from Thomas [1]*

can reasonably be measured at sub-millimeter amplitudes. Surveying techniques ranging from satellite based photogrammetry to GPS augmented surveying round out the upper end of the scale.

Thomas’s figure was not intended as comprehensive or definitive, but to illustrate that measurement technology follows a roughly diagonal band on the plot, leaving two large inaccessible areas: large amplitudes at short wavelengths and small amplitudes at long wavelengths. The latter is populated with large precision optics, assuming that the “amplitude” refers to departure from a curved datum, such as the spherical or aspheric wavefront from an interferometer. The primary mirror segments for the Giant Magellan Telescope are a long wavelength example.

Large amplitudes at short wavelengths imply large slopes with few applications to precision optics, with the possible exception of sub-wavelength periodic structures. There are many applications in biology, many biological structures from the cellular scale up through forest canopies have large vertical and small horizontal dimensions [1].

In 2007, Jones and Leach investigated in-plane scanning speed as a metrological characteristic for an area profiler. This constraint was used to derive a 3-D Stedman diagram, presenting a polygon outside of which the instrument cannot measure. Parameters relevant to contact-based measurement were added to the diagram, including the limitations of the speed at which the probe tip can traverse the surface, maximum

and minimum probe force, and tip geometry. The drawback of this approach is that a large amount of information is presented on one plot. This, combined with the log-log nature of the diagram, makes a static 3-D polygon difficult to extract quantitative information from. This may be mitigated somewhat through projected orthogonal views, or the use of a tool such as MATLAB to facilitate analysis of the workspace.

Rosen and Thomas's 2013 paper took a different approach to the idea of a 3-D Stedman diagram, adding measurement time as a third axis. This attempt has the same disadvantage - quantitative information about measurement time is difficult to derive from the graph.

METHODS TO CAPTURE NUANCE

One shortcoming of the original Stedman diagram is its binary nature. It is composed of a two dimensional polygon that represents the instrument's measurement limitations. It implies that inside the polygon a measurement will be successful, outside it will not. The situation is not so cut and dried. For example, the LIGO input and end test masses have radii of curvature ~ 11 km which were measured using traditional interferometry with a ~ 400 mm aperture. The Fourier construction in the lateral dimension implies, incorrectly, that low amplitude quadratic form departure is outside the performance envelope of interferometric and other tools. Clearly such departures can be detected, with increasing uncertainty, at apertures down to the limit imposed by detector noise.

Inside the polygon, take the example of an interferogram of surface with high spatial frequency that is barely within the Stedman envelope. The instrument transfer function provides insight into the behavior of the SWLI, at least when within the "smooth surface" domain [11]. As spatial frequencies approach the Nyquist limit the fraction of the amplitude reported decreases. Put differently, the uncertainty in those reported frequencies increases significantly. While the instrument returns a measurement, it is of higher uncertainty than longer wavelength measurement data would be.

These are not the only examples of "edge case" behavior of the Stedman diagram; they are only used to illustrate the binary nature of the diagram and the disconnect it has with the

reality of measurements taken near the instrument limits.

This presentation proposes some ideas which can be used to add nuance to the Stedman diagram, developed using a few guiding principles. The first, discussed above, is that all measurements have an associated uncertainty and that uncertainty often increases near the instrument's limits. The second, discussed in more detail below, is that the limits of a measurement instrument with regards to longer wavelengths are tied to the method of data analysis used.

For example, consider power spectral density. At least one cycle of the longest wavelength of interest should appear in the measurement, and three or more is preferred if phase is important. On the other end of the spectrum are lower order Zernike polynomials such as power, which can be fit to a quadratic. Any parameter that can be fit to a quadratic can be evaluated using an arbitrarily small aperture, in contradiction of the limits implied by Stedman.

The third principle is that the diagram should be easy to read and interpret, even if this means that information must be spread between different graphs, or included in tabular format. This idea was inspired by tools such as nomograms, bode plots, et cetera.

UNCERTAINTY

Uncertainty in results from surface measurement instruments is related to the metrological characteristics of the instrument [12]. Using this method it is possible to estimate some important contributors to the uncertainty in a measurement. Measurement uncertainty depends on the convolution of the surface and the instrument. When the surface being measured is taken under consideration the uncertainty can increase significantly.

Metrological characteristics directly influence uncertainty reported height, but calculation of uncertainty in measurement *parameters* using metrological characteristics is a more difficult task; the degree of contribution of a metrological characteristic to uncertainty in a parameter is not always clear. The primary metrological characteristics that influence surface topography instrument uncertainty are: measurement noise, flatness of reference datum, scale amplification and linearity, and resolution.

Two instruments currently in use at UNC Charlotte are used as examples in this presentation: a Mahr LD260 mechanical profiler and Zygo NexView Scanning White Light Interferometer (SWLI). Each instrument has different metrological characteristics that influence measurement limits and uncertainty.

SOME IDEAS FOR EXTENDING STEDMAN

The first example shows the effect of averaging multiple measurements using the SWLI with 2.75x objective. Averaging of multiple measurements reduces noise by improving the signal to noise ratio, allowing for lower amplitude surface features to be resolved. The noise floor of an interferometer decreases with $1/\sqrt{N}$, where N is the number of repeated measurements averaged.

Taking the manufacturer's stated noise floor of 0.12 nm and averaging over $N=100$ measurements gives a theoretical noise floor of 0.012 nm, although measurement uncertainty increases as that threshold is approached. This "fuzzy boundary" is shown below.

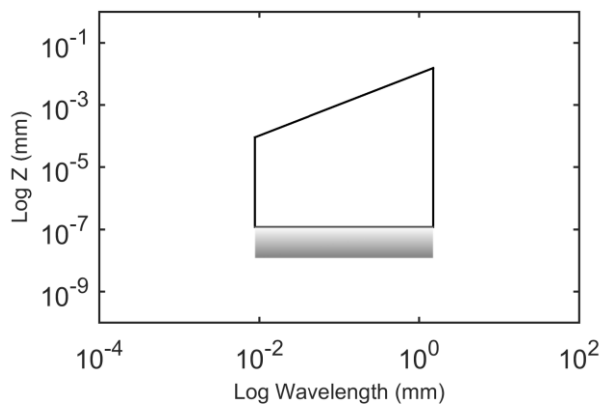


FIGURE 3. Diagram for SWLI including noise floor averaging

The NexView SWLI is also equipped with an interchangeable tube lens, allowing for magnification, or zoom, of the objective to be increased or reduced. This is convenient because it effectively gives the user 'half' sizes of objectives, allowing for greater field of view at half magnification, or less field of view for double magnification without changing fundamental optical limitations. For low-magnification objectives this can cause resolution to be detector limited instead of objective limited, which means that the resolution limit changes

significantly when moving from 0.5x zoom to 2.0x zoom using the tube lens. This is shown below for the 2.75x objective using overlapping Stedman diagrams.

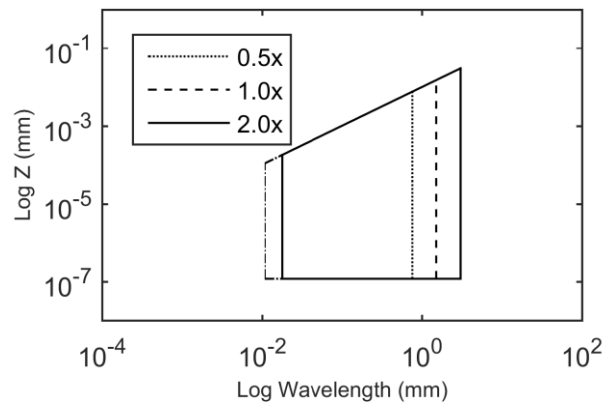


FIGURE 4. Diagram for SWLI showing limitations imposed by tube lens zoom

The next diagram shows the Mahr profiler with straightness error of the reference datum included. Straightness error here is modeled as quadratic with a maximum departure of 75 nm, based on the assumption that the reference flat was manufactured by pitch polishing methods. Measurements in the shaded area will have increased measurement uncertainty, with the maximum uncompensated straightness error occurring at half of the traverse length. Using this error model, when tilt is removed, maximum straightness error will always occur in the middle of the part.

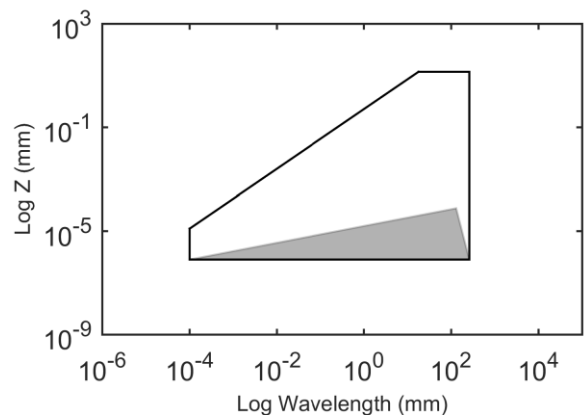


FIGURE 5. Diagram for profiler showing uncorrected straightness error

Measurement time has been added to the Stedman diagram before, but the next figure presents a novel method. The dashed box below the traditional Stedman represents the lateral range of the profiler when taking additional traces over a rectangular aperture. The amount of time required to complete a measurement increases linearly as a function of the number of traces in this case; this is represented by the diagonal line connecting the box.

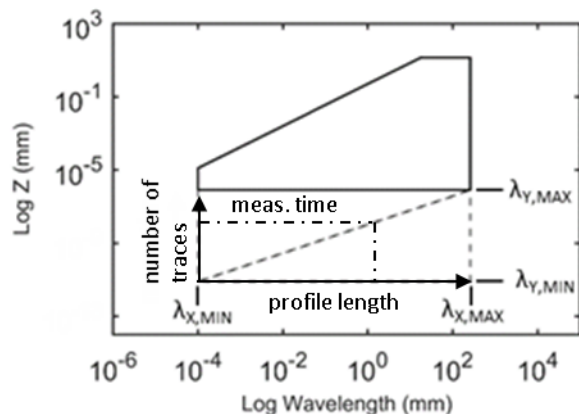


FIGURE 6. Diagram for profiler showing measurement time for multiple traces

The Stedman diagram's roots in the Fourier domain suggest a hard limit on radius measurements as well. When looking at large optics with large radii of curvature such as the Hubble telescope main mirror or the LIGO input and end test masses the lack of even one sinusoidal cycle inside of the measured aperture implies the wavelength falls outside of the Stedman diagram limits. However, they can clearly be measured. This is true for any aspect of form that can be modeled as a quadratic. Figure 7 shows a "quadratic" box to the right of the main Stedman. The instrument will measure surfaces with quadratic form inside the box as described above, but with increasing uncertainty as one moves to the right. The final figure shows a 'nomogram' style example for the profiler where measurement time, straightness error, and the quadratic box are all shown on the same plot.

The box on the right is defined in terms of aperture, with an arbitrary maximum bound of 100 times the maximum trace length of the profiler. Uncertainty is modeled as quadratically increasing with the square of the aperture of the optic being measured.

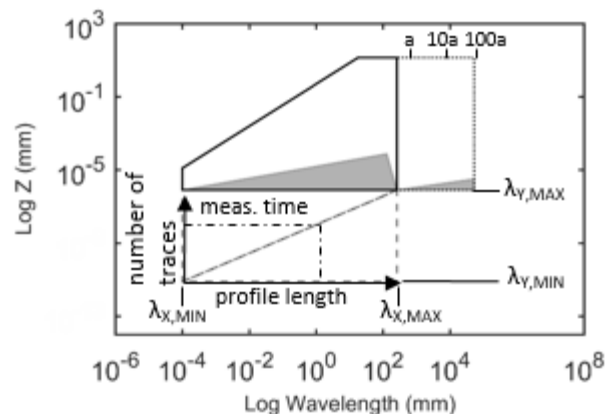


FIGURE 7. Diagram for profiler showing straightness error, measurement time for multiple traces, and extended range for quadratic form measurements

DISCUSSION

Since its inception in 1987 the Stedman diagram has seen spurts of innovation and development interspersed with long periods of silence, throughout which it has often been used as "window dressing" at conferences. This presentation seeks to stimulate the development of the Stedman diagram into an effective tool to allow optical designers to give more consideration to "design for metrology". In the past ten years aspheric optics have transitioned from specialized outliers to widespread acceptance and common usage; they were the fastest growing market segment of all optical elements in 2015 [13].

Aspheric optics manufacturing begs for tight integration of the process chain and concurrent engineering; design, production, and metrology should communicate requirements clearly. Freeform optics are now the newcomer on the scene; their increased complexity increases the need for design for manufacture, metrology, and assembly. That requires accessible information on the capabilities and limitations of all parts of the process chain. With this context in mind, it is important to create bridges of communication between designers and metrologists.

The ideas presented here are intended to stimulate discussion and spark new ideas. The Stedman diagram is a limited tool, but it is also easily extended to capture more nuances and metrological characteristics of an instrument. The plots presented seek to optimize the amount of visual data presented on one graph,

but this is also a limitation. Ideally a middle ground can be found where easy to interpret but information-dense Stedman variations – which may add data in non-graphical formats -- can be developed that allow optical designers, manufacturers, and metrologists to easily see current capabilities, limitations, and technology gaps.

ACKNOWLEDGEMENTS

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MEMS DEFORMABLE MIRRORS FOR ADAPTIVE OPTICS

Thomas Bifano¹

¹Boston University Photonics Center
Boston University
Boston, MA

INTRODUCTION

In optical imaging systems, aberrations of the object, the beam path, or the optics themselves can lead to deterioration in image quality. Adaptive optics (AO) is the science and technology of compensating such aberrations through dynamic reshaping of the wavefront to improve image quality.

In general, AO involves measuring the wavefront distortion at some plane in the imaging system, and then using a deformable mirror to reshape the wavefront at an optically conjugate plane. The deformable mirror can be classified by its number of independent actuators, each actuator's range and resolution of surface-normal motion, and the dynamic bandwidth of the actuation.

AO has found two major applications in modern optical imaging systems: Telescopes and retinal imaging systems. In large telescopes, the primary source of aberrations is index of refraction variations in the atmosphere that vary across the projected aperture of the primary mirror. In retinal imaging systems, the main source of aberration is shape errors in the optics of the eye's cornea and lens. A third AO application area that has emerged more recently is microscopy. In microscopes, the aberration is often associated with the object itself, or with differences in index of refraction of the medium through which the object is imaged and the index of refraction for which the objective was designed.

In broad terms, telescopes require hundreds or thousands of actuators, several micrometers of stroke, sub-nanometer precision, and kilohertz bandwidth. Retinal imaging systems require about a hundred actuators, tens of micrometers of stroke, ten nanometer precision, and tens of hertz bandwidth.

A challenge is how to design the deformable mirror. Our approach has been to use semiconductor fabrication techniques, building

the DM actuators and mirror through surface micromachining, a process involving sequential thin film deposition, lithographic patterning, and plasma etching.

Our DM architecture is based on a scalable array of parallel plate electrostatic actuators, fabricated in silicon through semiconductor batch processing. Each square actuator plate is rigidly connected to the substrate along two of its edges, and is suspended above an addressable electrode. Voltage applied to that electrode imposes an electrostatic attractive force on the electrically grounded actuator plate, causing it to bend toward the substrate in proportion to the square of applied voltage. Each actuator plate has a central post connected to a continuous or segmented mirror layer (Fig 1.)

MEMS DM DESIGN AND MANUFACTURE

MEMS-DM devices are both more economical and more capable than the prior state-of-the-art, which involved glass optics bonded to arrays of piezoelectric actuators. These advantages are attributable to their design and their manufacturing:

Design

- The devices are scalable: increasing the size of the DM and its spatial resolution is achieved by adding copying and pasting lithographic mask features.
- The device is mechanically stiff and has low mass, allowing control bandwidths of tens of kilohertz (Fig 2) .
- The non-contact electrostatic actuation mechanism is repeatable to sub-nanometer precision, consumes almost no power, exhibits no hysteresis, and is unaffected by trillions of cycles of operation.

Manufacturing

- The production approach does not call for exotic materials or manufacturing tolerances: it can be made using a MEMS

foundry and begins with an optically smooth, flat, and inexpensive substrate.

- Devices are batch produced twenty wafers at a time, so that while development costs are high, commercial production and replication costs are low.
- Hundreds of devices can be produced on each wafer, allowing broad parameter variation in a single batch production cycle. This accelerates research and prototyping.

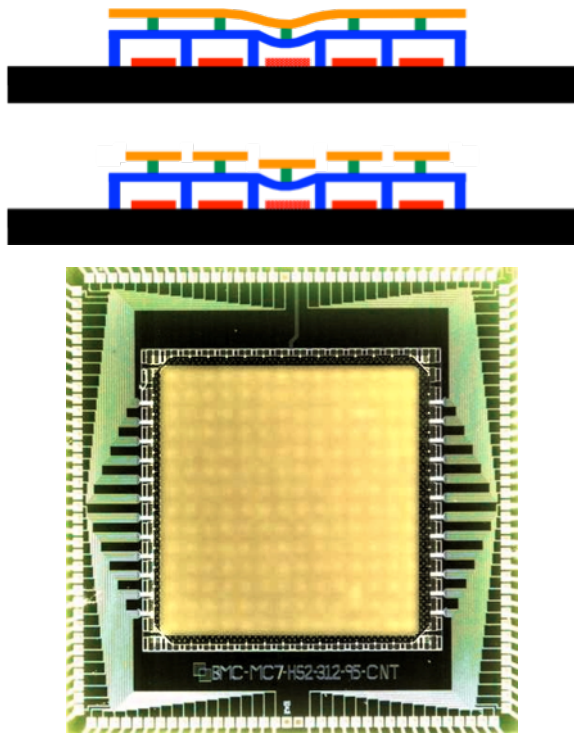


FIGURE 1. Top: Schematic cross section of continuous and segmented MEMS DMs. The mirror (gold) is connected by posts (green) to an array of electrostatic actuator membranes (blue). Actuator membrane deflection is approximately proportional to the square of voltage applied to the rigid electrodes (red) on the wafer substrate (black). Electrostatic attraction between the energized actuator electrodes and the electrically grounded actuator membrane deflects the actuators precisely and repeatably, shaping the mirror. All structural elements are silicon. **Bottom:** Photograph of a fabricated DM, with 140 active actuators (and two inactive buffer rings) supporting a continuous DM. The active area of this DM spans 4 mm.

Perhaps the most important differentiator, however, is cost. MEMS DM systems inclusive

of driver and software cost almost an order of magnitude less than the technology they displace. This economical advantage has catalyzed rapid growth in the field of adaptive optics, both within its original astronomical instrumentation domain and in other imaging

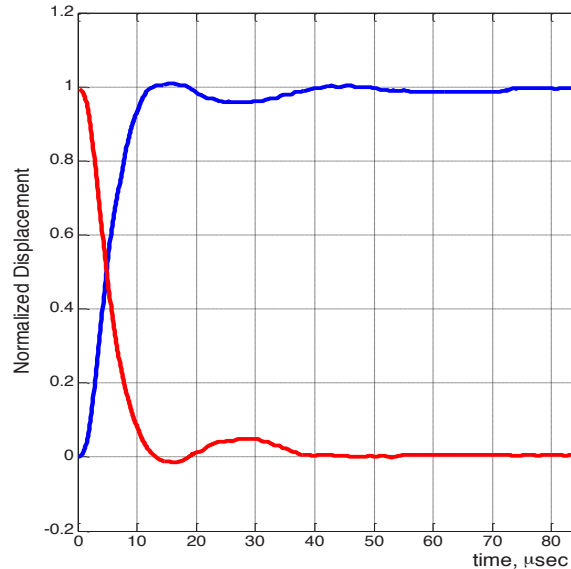


FIGURE 2. Surface normal deflection of DM measured as a function of time in response to step inputs (up and down) in voltage on an electrostatic actuator. Damping is provided by viscous motion of the squeeze film of air between thin film layers. The settling time is about 15 μ s.

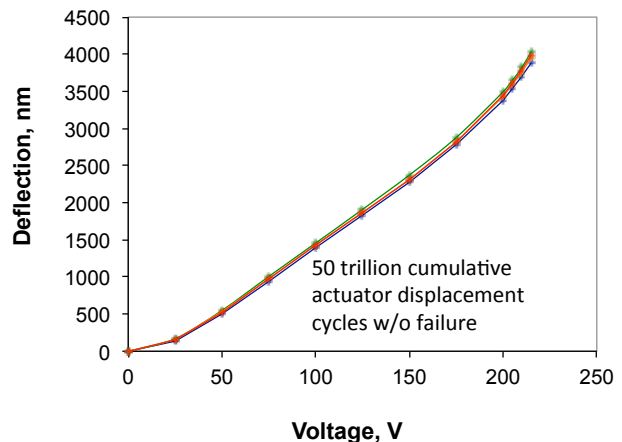


FIGURE 3. Applied actuator voltage vs mirror deflection. Actuation is nonlinear, but repeatable to within measurement tolerances of the interferometer used for evaluation. There is no measurable hysteresis, and actuation is unchanged by trillions of cycles of motion.

applications for which aberration compensation is critical. Highlights of those applications include *extreme adaptive optics* for high-contrast astronomical imaging, *ophthalmic adaptive optics* to improve *in-vivo* retinal imaging systems, and *adaptive optics microscopy* to improve subsurface imaging of biological tissue.

DMS FOR AO IN LARGE TELESCOPES

Over the past decade, AO has become indispensable as a means to compensate aberrations introduced by atmospheric turbulence in large ground-based telescopes. The resulting gains in resolution are especially important in narrow-field imaging with large telescopes and have led to exciting recent advances in observation of exoplanets, characterization of planetary rings and atmospheres, and studies of galactic structure. AO will become increasingly important for the coming generation of extremely large telescopes (ELTs) since its benefits increase nonlinearly with telescope aperture.

“Extreme” adaptive optics – the direct detection and characterization of extrasolar planets through coronagraphic imaging or spectroscopy combined with high-performance AO – is a key driver for development of advanced MEMS deformable mirrors. BMC recently produced a 4092-actuator, 26mm aperture, 4 μ m stroke MEMS-DM for a new planet-imaging instrument known as the Gemini Planet Imager (GPI). With closed-loop control, the DM has been flattened in the LAO test bed to within a 0.54nm in its controllable band of spatial frequencies, and 13nm RMS overall [2] (Fig. 4).

One important feature of the MEMS DMs is that they can be shaped predictably in a single step to nanometer-scale precision, enabling open loop AO control. That characteristic allows profound advances in AO. If control can be achieved in open loop, then promising new astronomical instruments can be implemented for the first time, including especially multi-object adaptive optics (MOAO). Controlling a MEMS DM, in open or closed loop, is complicated by the fact that independently addressed actuators are mechanically coupled to one another through the mirror facesheet. As a result, the displacement of any DM actuator alters the forces experienced by its neighboring actuators. An efficient open-loop control algorithm can be achieved if one first

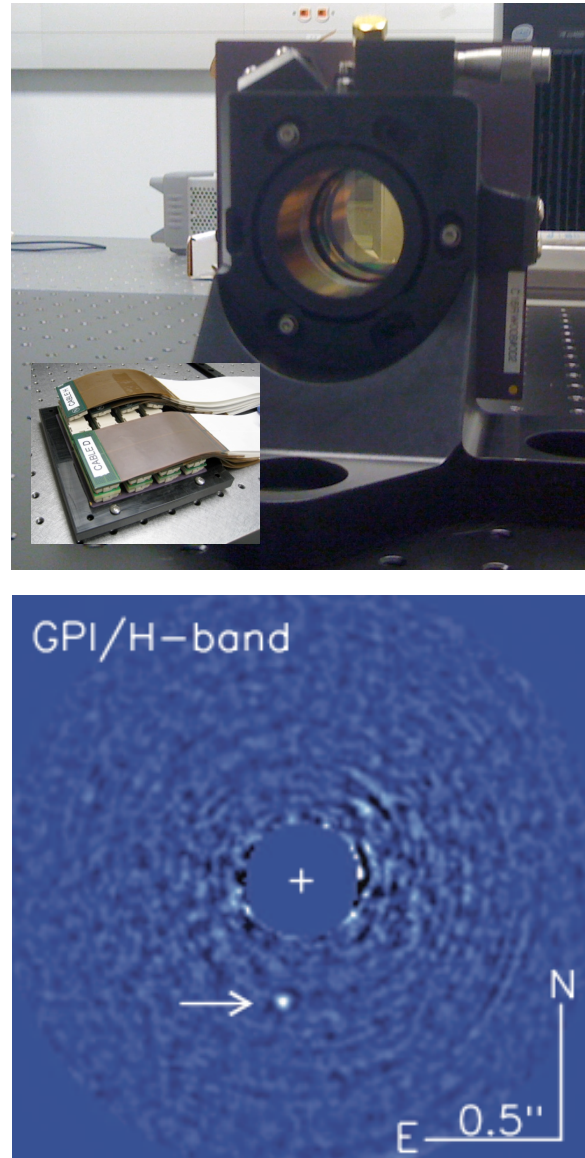


FIGURE 4. Top: Packaged 4092 actuator DM used in the GPI coronagraph instrument installed at the Gemini South Telescope in Chile. This instrument was designed to detect Jupiter-like young planets. Bottom: An image of a Jovian young planet (51 Eri b) that was imaged and characterized by GPI [1].

establishes a DM model that consists of two coupled mechanical subsystems: the continuous facesheet and the array of actuators connected to the facesheet via rigid posts. The rigid posts are the points of connection for the two subsystems. This can be seen in the free body diagram of Figure 6, where F_M is the force

imparted by the mirror facesheet, F_A is the force imparted by the actuator diaphragm, and F_E is the electrostatic attractive force associated with an applied actuator voltage (Fig 5).

The DM mirror layer (facesheet) is modeled as a thin plate free on all edges undergoing both stretching and bending. For displacements smaller than the facesheet thickness, the deformation is dominated by linear elastic bending as described by the biharmonic thin plate equation. As displacement approaches the thickness of the facesheet, in-plane strain is no longer negligible and the facesheet begins to stretch as well as bend.

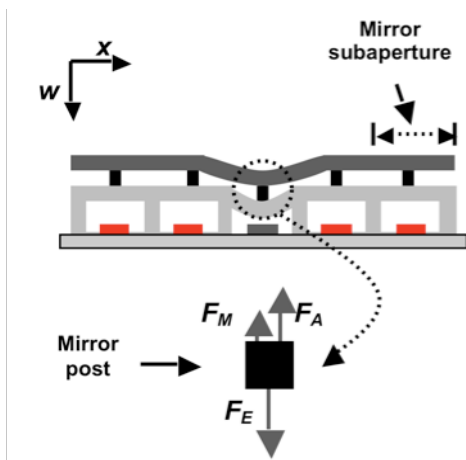


FIGURE 5. Equilibrium force balance at actuator posts allows open loop actuation by balancing the electrostatic actuation force, the actuator membrane mechanical restoring force, and the mirror coupling force.

DMS FOR AO IN RETINAL IMAGING

In vivo retinal imaging using AO enhanced ophthalmoscopes was a key driver for MEMS DMs development. The motivation was that uncompensated aberrations of the eye generally prevent ophthalmoscopy instruments from resolving cellular structures on the retina. On the other hand, the diffraction limit for the eye's optics *would* allow cellular resolution if those aberrations could be compensated. A pioneering effort by David Williams' research group at the University of Rochester demonstrated closed loop AO in 1997, yielding substantial improvement in image quality and the first clearly resolved images of cone photoreceptors. A decade of subsequent research by many research groups yielded

numerous additional advances, including integration of AO in confocal SLO and optical coherence tomography (OCT) instruments, and development of MEMS deformable mirrors designed specifically for use in retinal imaging AO.

The capacity of confocal AO systems to resolve cellular structures and microvasculature led to rapid expansion of AO in retinal instruments. The aperture (4.4mm), stroke (6 μ m), and actuator count (140) of our MEMS DMs were specifically designed to compensate of aberrations of the eye. AO enhancement has resulted in the sharpest images of microscale features in the living retina ever obtained (Fig

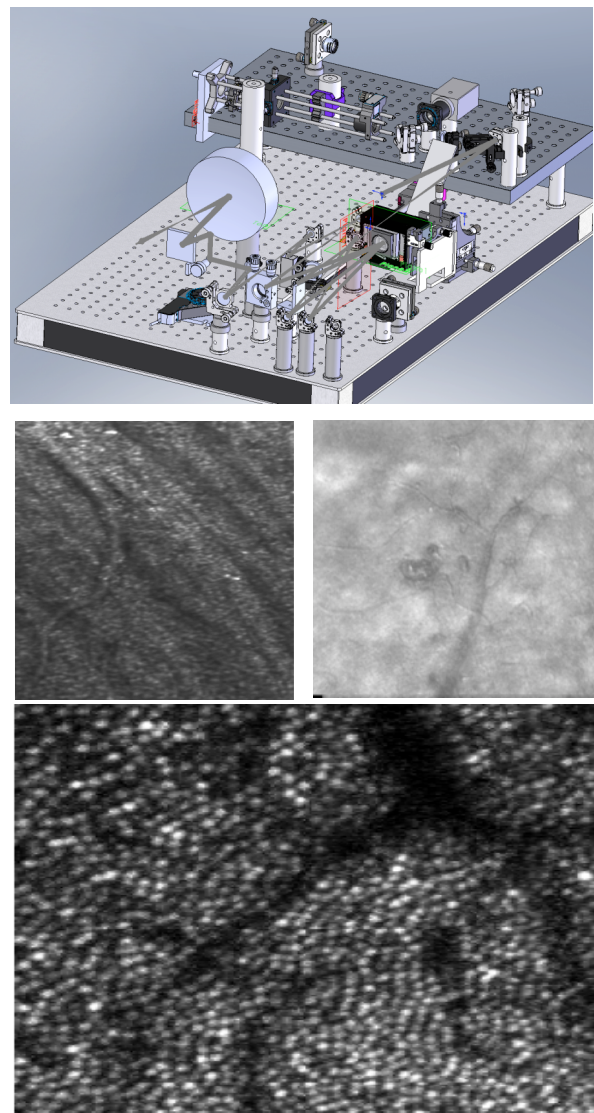


FIGURE 6. **Top:** Schematic of an AOSLO incorporating a MEM DM. **Middle:** AOSLO images of the nerve fiber layer and toa vascular microaneurism. **Bottom:** AOSLO image of cone photoreceptors.

6).

Such imaging has the potential to replace or augment less direct methods of tracking disease state, and can resolve retinal structures and pathology that should be valuable in both diagnosis and treatment. Images representative of performance achieved are shown in Figure 4. This instrument will be used in the coming year to evaluate clinical usefulness of enhanced resolution, contrast, and brightness in monitoring the progression of disease and the effectiveness of treatments that impact cellular structures in the retina.

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4DoF Active Optical Alignment Tool

Thomas Haase, Bernhard Guddei, Peter Diez
Product Division Magnetic Drives PIMag
Physik Instrumente (PI) GmbH & Co.KG
76228 Karlsruhe, Germany

INSTRUCTIONS

High precision micro- and nano-positioning stages are used for a wide variety of markets and industries. Example applications include beamline instrumentation, optical metrology, microscopy, photonics, life-science and medical technologies. A large number of those stages are used in combination with light sources and lasers for scanning and focusing applications and are therewith embedded in more complex opto-mechatronic devices that require e.g. sharp images. In such opto-mechatronic systems requirements with regard to high throughput, dynamics and highest precision motion technology are often of secondary importance. First and foremost, the performance of the optical path must be ensured. Therefore specifications such as repeatability, lifetime, operating accuracy of the guiding as well as the absolute position of the optical interface are more important. Operational accuracy include flatness, straightness and rotational tip/tilt motion requirements, mostly separated in absolute deviation and repeatability (e.g. when refocusing).

Breaking down optical requirements into positional and geometric tolerances for the nano-positioning stage is required due to the fact, that for the stage manufacturer an optical axis is a virtual line that cannot be grasped or seen during the assembly process of the positioning stage. Another difficulty is that the resulting optical performance over full travel range is hard to verify for the stage manufacturer without having access to the optical path or system.

Thus, meeting such high demands from the mechanical point of view is a great challenge. Often and despite manufacturing devices and highest production quality it cannot be guaranteed that the optical interface will fulfill all static and dynamic requirements after assembly, especially over full travel range.

The main focus of this paper is on the design of an active alignment tool to overcome

mechanical and assembling process limits while adjusting the position of a mechanical interface for an optical axis. In this paper, an approach of how to combine machine vision with micrometer resolution, precise static and dynamic tip/tilt motion measurement and PI internal Hexapod capabilities is shown. It is discussed how to reasonably split up an exemplary high precision, long stroke voice coil actuator into different subassemblies for an active alignment to reach outstanding operational accuracy of the optical interface. Results, limits and technical performance of the alignment tool are discussed and compared against conventional, original limits.

THE ACTIVE ALIGNMENT TASK

An optical axis has to be aligned to ensure best optical performance. Deviations from the ideal position and orientation are defined using static and dynamic boundary conditions or rather tolerances. As shown in Figure 1 the position and orientation of an optical axis is described relative to the stage interface, a customer coordinate system or often with the help of different orthogonal surfaces [A], [B], [C] of a stage itself.

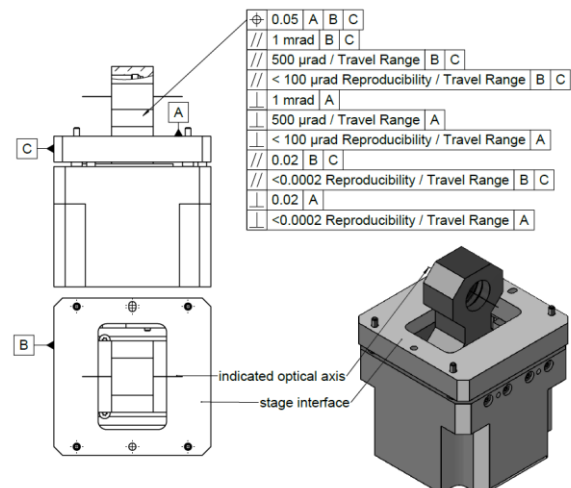


FIGURE 1. Positional Tolerances of the optical axis relative to the stage interface, defined by [A],[B] and [C] – three orthogonal surfaces of the stage

In Figure 1, the reference surfaces [A], [B] and [C] define a stage coordinate system [SCS]. Relative to this SCS, the optical axis has 4 degrees of freedom. Assuming the optical axis in the X direction of the SCS, the optical alignment has to be done for two lateral (Y, Z) and two rotational (rotY, rotZ) DoF. If the Home-Position of the stage itself is defined within the SCS, 5 degrees of freedom are specified.

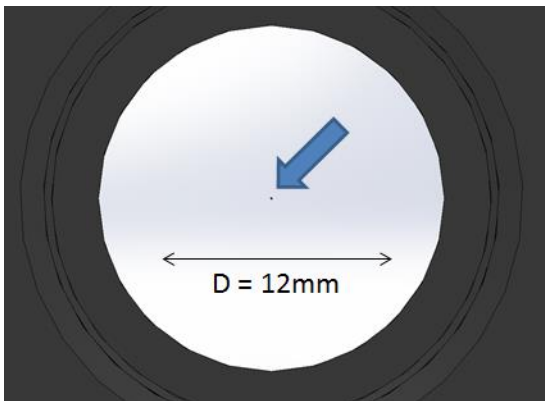


FIGURE 2. Position of the optical axis including its absolute lateral error band of $\pm 25\mu\text{m}$. Dynamic deviations shall not exceed this cylinder (specified with $\pm 10\mu\text{m}$) within its travel range of $\pm 1.1\text{mm}$

Depending on e.g. life time requirements, limited assembly space or reproducibility specifications different guiding systems are feasible. In the stage of Figure 1 a customized Robert's straight line mechanism flexure hinge guiding was developed due to high lifetime and reproducibility requirements. The guiding is one of the most critical and complex components of a stage and has a high effect on the position and orientation accuracy of the resulting optical axis. Depending on the customized, long stroke guiding-design, mechanical manufacturing tolerances as well as additional mounting tolerances limit the accurate positioning. Furthermore, the dynamic specifications can only be evaluated after the stage is assembled. Thus, high - precision static and dynamic customer specifications cannot be assured and a stable production is not guaranteed, Figure 2.

Nevertheless, to be able to align the optical axis relative to the defined SCS it is necessary to split up the actuator into two different subassemblies as shown in Figure 3. A first simple unit contains only the well-known stage interface. The second unit is something like a

stand-alone stage including the actuator, sensor and guiding. By splitting up the stage into those two subassemblies it is possible to align them relative to each other. The stand-alone unit allows closed loop motions over full travel range during the alignment process. Therewith it is possible to monitor and influence also dynamic specifications. Such an active alignment requires high-resolution measurement equipment and the possibility to measure simultaneously static and dynamic specifications with regard to lateral and rotational degrees of freedom.

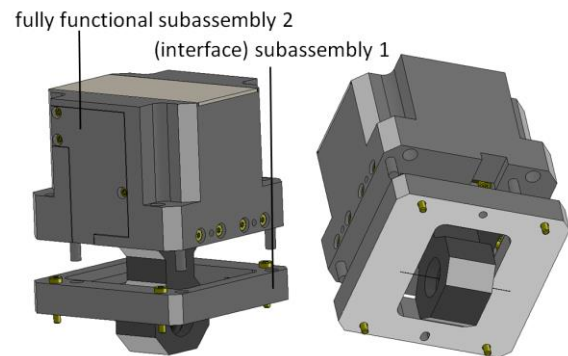


FIGURE 3. Subassemblies for an active alignment: 1st subassembly includes the stage interface; the 2nd subassembly is a functional stage including sensor, guiding and actuator

ALIGNMENT SYSTEM REQUIREMENTS

As described, two lateral and two rotational degrees of freedom have to be aligned relative to a stage or rather customer interface. All 4 DoF are specified using both static and dynamic boundary conditions. The basic idea of the alignment tool is to measure all 4 DoF at the same time with the help of a camera system and an autocollimator (AKF). To be able to measure synchronously with both instruments, a double-sided dummy objective including a mirror for the AKF and an optical marker for the camera is required. Figure 4 illustrates the fundamental dummy objective design. The mirror is internally preloaded by a spring to fit perfectly to the specified surface of the optical axis of Figure 1. A principal setup of the alignment tool is shown in Figure 5. It shows the optical alignment tool including the camera, the AKF and a PI-Hexapod H-811 for aligning the stage relative to the customer interface.

Focus of the alignment tool is the customer interface (in blue color of Figure 6), which can be described as counterpart to the stage

interface. The 1st subassembly from Figure 3 is mounted directly onto the customer interface. The 2nd subassembly is equipped with the dummy objective from Figure 4 and actively adjusted using the hexapod. Lateral static and dynamic accuracies are measured using a COGNEX In-sight 5705 camera system in combination with a telecentric lens. The camera offers high performance with high resolution making it the fastest 5MP self-contained vision system in the world [1]. To be able to measure the lateral drift of the optical axis and therewith against dynamic specifications it is necessary to move the stage during the alignment process. Because of this, the measuring distances between the stage and the camera changes. By using a telecentric lens, an autofocus is not required.

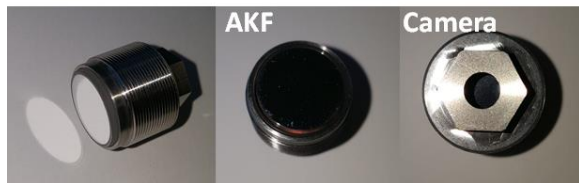


FIGURE 4. Dummy objective including the mirror for the AKF and a camera hole for determining the lateral position of the optical axis

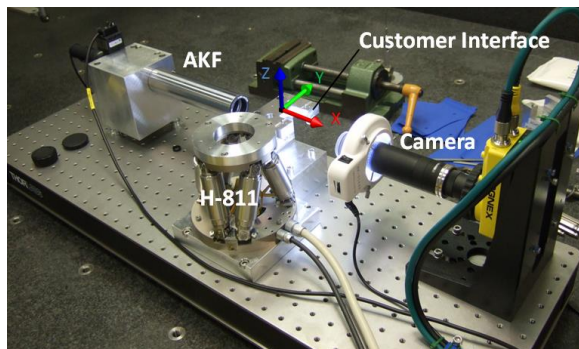


FIGURE 5. Illustration of the alignment tool: an autocollimator, a camera and a PI Hexapod H-811.D2 are used to actively adjust the position and orientation of the optical axis relative to the customer or rather stage interface.

The camera image has a resolution of 2448 x 1948 pixels with a resolution of 3.85 μ m/pixel. COGNEX offers an 'insight vision system' software that additionally allows high interpolation rates while measuring the position of an object. The lateral image noise level is determined to $\pm 0.5\mu$ m. The rotational tip/tilt angels are

measured using an OEG-AKF 300/40 with an accuracy of ± 0.8 arcsec and a measuring range of 0.7° / 0.5°. The adjustment is done with the help of the hexapod H-811.D2. An extract from the data sheet is shown in Table 1.

TABLE 1. Technical data of the H-811.D2 (extract from the data sheet)

H-811.D2 6-Axis Miniature Hexapod	Value	Unit
Min. incremental motion X,Y	0.25	μ m
Min. incremental motion Z	0.1	μ m
Min. incr. Motion $\theta_x, \theta_y, \theta_z$	3	μ rad
Repeatability X,Y	± 0.15	μ m
Repeatability Z	± 0.06	μ m
Repeatability θ_x, θ_y	± 2	μ rad
Repeatability θ_z	± 3	μ rad

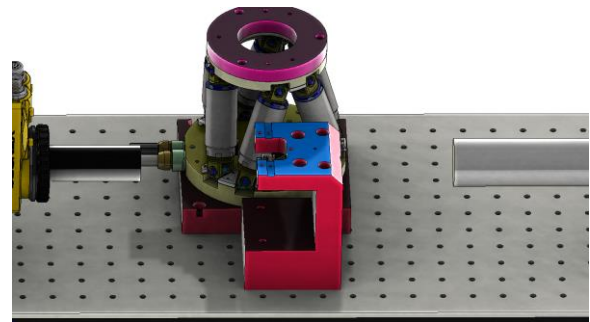


FIGURE 6. Determining the full position and orientation of the customer interface (surface in blue) with the help of a Zeiss Accura Bridge-Type Measuring Machine (KMG)

SYSTEM CALIBRATION

For absolute lateral and rotational measurements it is necessary to ensure that all instruments are aligned perfectly relative to the customer interface as shown in Figure 6 (blue). Due to this, all instruments of the optical alignment tool need to be calibrated into the system. The calibration process requires a high resolution measurement system with absolute position output. A Zeiss Accura Bridge-Type Measuring Machine (KMG) including a VASTxT contact sensor with an absolute accuracy of $\pm 1.6\mu$ m is used for calibrating the setup. Figure 8 shows a picture of the overall calibration setup. Following steps have to be carried out sequentially for aligning the setup:

1. All measurement instruments of the alignment tool are preassembled and roughly aligned as shown in Figure 5.

2. The absolute 3D coordinate system of the customer interface is determined. Here, the KMG is required to probe different points on three orthogonal planes as shown in Figure 6 (blue surface).
3. The calibration fixture from Figure 7 is mounted to the bracket and on top of the hexapod. It includes the dummy objective for aligning the camera and a mirror for the AKF.
4. With the help of the Hexapod the dummy objective is moved into its exact, theoretical position relative to the customer interface, Figure 8. This position is identical with the optical axis and specified by the customer drawing from Figure 1. The KMG is required to measure the exact position directly on the surfaces of the dummy objective to eliminate positioning errors of the hexapod itself. The position and orientation within the hexapod coordinate system is stored.
5. A second point along the optical axis is aimed and its relative position is stored within the hexapod coordinate system. It is important that both points (step 4) have exact the same relative orientation in space to each other.
6. The Hexapod is now able to move linear between both measuring points. Due to this motion along the ideal optical axis, the camera can be aligned. While moving the dummy objective back and forth, the camera is adjusted until no more relative motion can be measured from the point of view of the camera, Figure 8.
7. In a next step the orientation of the mirror of the calibration fixture from Figure 7 has to be aligned relative to the customer interface. Therefore, the KMG probes different points directly on the surface of the mirror. Based on this, the orientation of the AKF is aligned into the setup.

At the end of the calibration both the camera and the AKF are aligned and therewith coaxial to the optical axis. Now it is possible to measure absolute static and dynamic deviations. Table 2 illustrates sources of errors and the precision of the alignment tool.

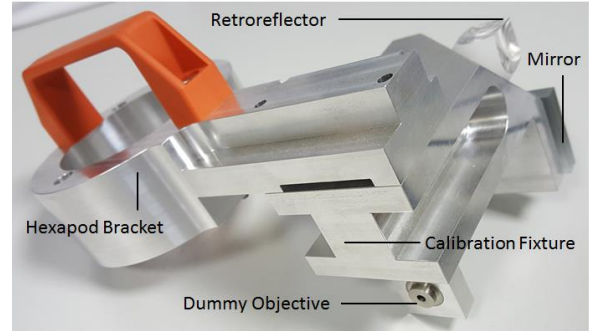


FIGURE 7. Hexapod bracket and Calibration fixture including the retroreflector, the mirror and the dummy objective for the camera alignment

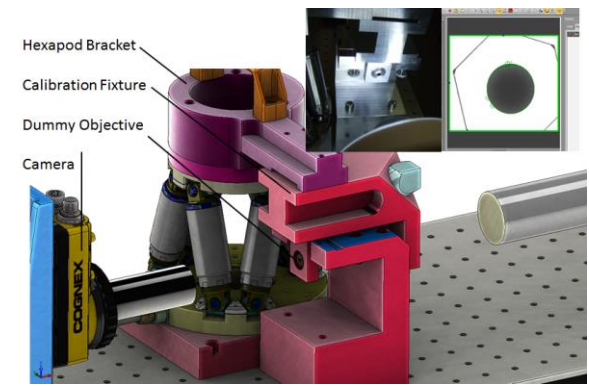


FIGURE 8. Calibration setup

TABLE 2. Measurement Instruments and Potential Sources of Errors

Measurement Instrument	Lateral	Rotational
KMG	$\pm 1,6\mu\text{m}$	-
H-811.D2 Hexapod	$\pm 0.25\mu\text{m}$	$\pm 3\mu\text{rad}$
image processing resolution	$\pm 3.85\mu\text{m}$	
image processing noise	$\pm 0.5\mu\text{m}$	
OEG-AKF resolution	-	$\pm 3.9\mu\text{rad}$
misc. Error	$\pm 1.0\mu\text{m}$	$\pm 3\mu\text{rad}$
overall accuracy	$\sim \pm 7.2\mu\text{m}$	$\sim \pm 10\mu\text{rad}$

MEASUREMENT ROUTINE

Subassembly 1 is mounted onto the customer interface. With the help of the bracket and the hexapod, the fully functional subassembly 2 is moved into its theoretical ideal position above subassembly 1, Figure 9. The stage is moving back and forth within its full travel range along the X axis and its optical axis. The camera and the AKF measure the static and dynamic deviation. The position and orientation of subassembly 2 is adjusted until all specifications are fulfilled and the absolute and the relative,

stage-depending optical axis are almost identical and coaxial. Both components are glued together using glue with low shrinkage properties and/or additional properties. Thereby, the available stiffness of the hexapod is valuable to keep subassembly 2 in its ideal position during the hardening of the glue.

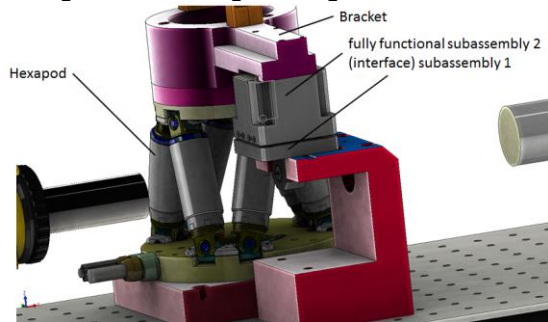


FIGURE 9. Measurement Routine

RESULTS

The active alignment tool was assembled, calibrated and applied successfully to align the optical axis of a customized nano-positioning stage with a travel range of $\pm 1.1\text{mm}$. Table 3 and Figures 10 to 11 present achieved alignment results after adjusting and gluing both stage-subassemblies together. Table 3 also compares the results of the customized Robert's straight line mechanism linkage with a standard PIFOC® objective scanner P-721 including a short stroke linear guiding based on wire cut EDM technology,

TABLE 3. Summary of achieved results based on 3 measured stages and determined for a travel range of 2.2mm . The Results are compared against a P-721 PIFOC® Objective scanner with a travel range of 0.1mm

	Spec.	Stage #1..#3	P-721	Unit
travel range	2.20	2.2	0.1	mm
abs. tip/tilt	± 500	± 313	± 1000	μrad
dynamic tip/tilt /travel range	± 250	± 57	± 13	μrad
tip/tilt repeatability	± 50	± 1.5	-	μrad
abs. lateral deviation	± 25	± 17	± 150	μm
dynamic lateral dev. /travel range	± 10	± 6.85	± 0.1	μm
lat. repeatability	± 0.1	± 0.004	± 0.005	μm
lifetime	10^9	$>10^9$	$>10^9$	

Process reliability and stability have been the main focus of the development of the active alignment tool. These functionalities have been successfully tested and implemented.

Additionally it has turned out that a big advantage of the tool is the minimized time effort for such a stable and reliable aligning process. Only minutes are required to adjust the position and orientation of both subassemblies to fulfill all specifications. Furthermore, this alignment and its fundamental functionality are transferable to different stage types. Depending on the measurement resolution of the instruments higher accuracies are feasible.

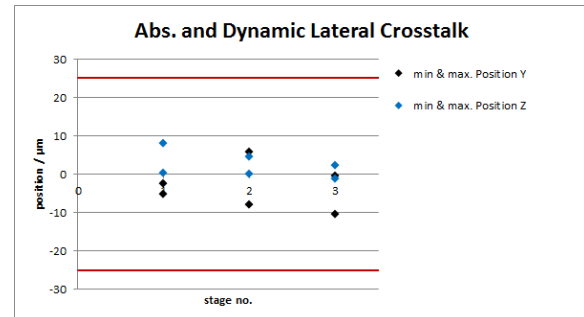


FIGURE 10. Lateral Crosstalk Results

A disadvantage of the tool is of course the risk of additional impacts due to the required adhesive. The adhesive selection is a very important aspect of the final alignment quality and may influence the final application. Adhesives are temperature-depending and can lead to distortion of the optical axis. At PI, low shrinkage adhesive is used to minimize the drift of the stage during hardening. Exemplary, Figure 11 illustrates non-symmetric stage crosstalk due to curing forces. Depending on the customer application different adhesives are required.

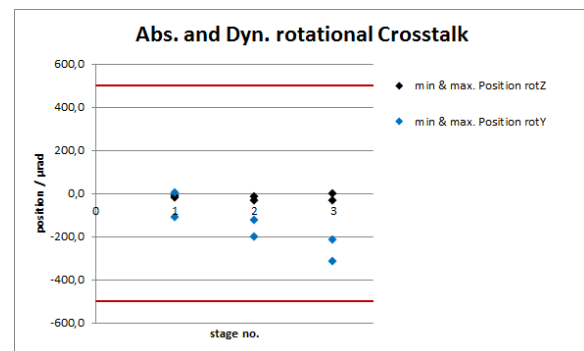


FIGURE 11. Rotational Crosstalk Results

REFERENCES

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TIP-TILT VIBRATION COMPENSATION: A SYSTEM ANALYSIS

Andreas Sinn, Thomas Riel, Florian Deisl, and Georg Schitter
Automation and Control Institute (ACIN)
TU Wien, Vienna, Austria

INTRODUCTION

Mechanical vibrations lower the performance of optical metrology systems used for astronomical applications as well as free-space optical communication. Both applications require precise and steady pointing. The required pointing precision of these systems is in the range of below $1 \mu\text{rad}$ [1]. Tip-tilt aberrations caused by vibrations are the most dominant type of aberrations in such systems [2]. In order to optimize the image quality or the communication bandwidth an active compensation system is required. One option for reduction of tip-tilt distortions is the usage of fast steering mirrors (FSM) [3]. In combination with a 4-quadrant photo-detector, measurement of tip-tilt distortions is possible and feedback control can be applied [3]. Feasibility and performance of this approach has been shown various times in literature [4, 5, 6, 7]. However, a detailed, systematic analysis of all required components of such a compensation system in terms of limiting precision may allow further increase of the metrology system's angular precision.

This paper investigates the precision of tip-tilt compensation systems for optical metrology platforms and systematically analyses the achievable angular precision. Dynamic error budgeting (DEB) is utilized as tool for systematic system analysis [8]. A compensation system based on a FSM in combination with a 4-quadrant photo-detector is used as tip-tilt sensor in order to measure and mitigate the influence of external vibrations. Based on the analysis' results a PID feedback controller is designed, which enables a significant reduction of the pointing error of the system.

SYSTEM DESCRIPTION

The evaluated system, shown in Figure 1, utilizes a 10" Schmidt-Cassegrain telescope. It is supported by an alt-azimuth mount which is mounted onto a mobile platform. No special precautions towards vibration damping are applied. An optical plate which carries the optical setup is attached on top of the telescope. The measurement target is a retro-reflector mounted onto a voice coil actuated shaker, which generates arbitrary vibra-

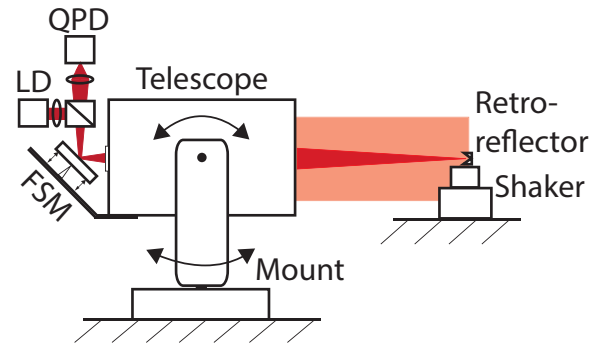


FIGURE 1. Schematic overview of the used optical metrology system. The tip-tilt compensation system with laser source (LD) is attached to a telescope system. A retro-reflector mounted onto a shaker is used as measurement target.

tional distortions. Coarse pointing is done via the telescope mount. A retro-reflective system configuration is chosen to simulate typical target applications such as transmission and/or reception of an optical guide-star as well as communication receivers.

A laser diode (CPS196, Thorlabs) with a wavelength of 635 nm is used as light source. Its beam is filtered to approximate a Gaussian beam shape and expanded to ensure low beam divergence. A 50:50 beam splitter separates outgoing and incoming light beams. Compensating actuator is a FSM (OIM101, Optics in Motion, Long Beach, CA, USA). This voice coil actuated mirror provides two compensation axes driven by integrated controllers. The retro-reflective target is mounted at a distance of 2 m from the telescope. A custom made four quadrant photo-detector (QPD) with integrated summing and subtractions amplifiers is used to detect tip-tilt deflections of the reflected laser beam. The feedback controller is implemented on a dSpace Rapid Prototyping System (DS1202, dSpace GmbH, Germany). Two 16 bit analog-to-digital converters (ADCs) are used to sample the output of the QPD. Two 16 bit digital-to-analog converters (DACs) provide the reference signals to the inputs of the FSM.

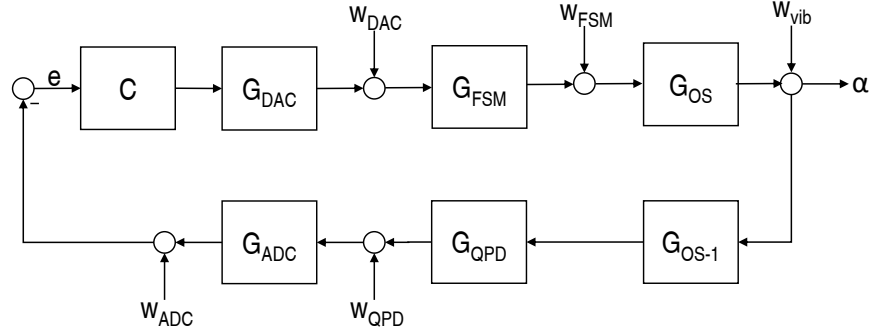


FIGURE 2. Schematic representation of the tip-tilt vibration compensation control loop. The individual components and the considered disturbance sources w_i are shown.

DYNAMIC ERROR BUDGETING

DEB allows the estimation of the total error of a system by modeling its dynamic error contributors. Stochastic disturbance sources are quantified by their PSDs and propagated through the LTI models describing the system components. Therefore, frequency dependent error budgeting is enabled and dominant error sources can be identified [8].

Theoretical background

The noise power per unit of bandwidth can be quantified by its single-sided PSD. The PSD of an unbiased time signal $w(t)$ corresponds to the its variance by

$$\sigma_w^2 = \int_0^\infty PSD_w(f) df, \quad (1)$$

where σ_w^2 is the variance of $w(t)$ and PSD_w the single-sided PSD of $w(t)$ [9]. The PSD at the output y of a system due to a specific disturbance w is calculated using the transfer function $T_{y,w}$ from the disturbance w to the output y and

$$PSD_{y,w}(f) = |T_{y,w}(f)|^2 PSD_w(f). \quad (2)$$

Under the assumption of uncorrelated disturbances w_i the overall PSD at the output y is superimposed by

$$PSD_y(f) = \sum_i |T_{y,w_i}(f)|^2 PSD_{w_i}(f). \quad (3)$$

To simplify the interpretation of the individual disturbance sources' contributions to the overall PSD at the output of a system, the Cumulative

Power Spectrum (CPS) is introduced with

$$CPS(f) = \int_0^f PSD(\nu) d\nu, \quad (4)$$

where PSD is a one-sided PSD. The end value of the monotone increasing CPS is equal to the variance of the signal, clearly indicating its contribution to the total value.

Component models

In this section all components shown in Figure 2 are modeled by their linear-time-invariant transfer functions and their PSD of the noise introduced by this component. As both axes of the compensation system use identical hardware all component models apply for both axes.

Analog digital converter

The ADC of the used dSpace system provides 16 bit resolution for a input voltage range of ± 10 V and a sampling rate of 1 MSps. As the controller algorithm is executed less frequently the ADC data is down-sampled to 20 kHz. First order low-pass filters with a cutoff frequency of 10 kHz are used as anti-aliasing filters. The quantization noise σ_{ADCq} is treated as zero-mean white noise, distributed up to the Nyquist frequency f_N [9]. With LSB_{ADC} specifying the voltage of the least significant bit this noise is modeled by

$$\sigma_{ADCq}^2 = \frac{LSB_{ADC}^2}{12}. \quad (5)$$

Electrical noise is measured to be $\sigma_{ADCe} = 0.7$ mV RMS. Since the noise sources are uncorrelated the total ADC noise is

$$\sigma_{ADC} = \sqrt{\sigma_{ADCq}^2 + \sigma_{ADCe}^2}. \quad (6)$$

The PSD of the ADC is calculated by

$$PSD_{ADC}(s) = |G_{ADC}(s)|^2 \frac{\sigma_{ADC}^2}{f_N}, \quad (7)$$

where G_{ADC} is the transfer function of the anti-aliasing filter.

Controller

A tamed PID controller is used for closed-loop operation of the tip-tilt compensation system [8]. Its transfer function is given by

$$C(s) = K_p + \frac{K_i}{s} + \frac{K_d s}{\tau_t s + 1}, \quad (8)$$

where K_p , K_i and K_d are the controller gains and τ_t is the time constant of the filter which limits the differentiating action at higher frequencies. The gains are not tuned for maximal bandwidth but more to achieve a compromise between robustness and disturbance rejection.

Digital analog converter

The quantization noise σ_{DACq} of the 16 bit DAC with a voltage LSB_{DAC} of the least significant bit at an output range of ± 10 V is given by

$$\sigma_{DACq}^2 = \frac{LSB_{DAC}^2}{12}, \quad (9)$$

assuming white noise up to the Nyquist frequency. The output sample frequency is 20 kSps. First order low-pass filters with a cutoff frequency of 10 kHz are used as reconstruction filters. The electrical noise is measured to $\sigma_{DACe} = 0.12$ mV RMS. With G_{DAC} as the transfer function of the reconstruction filter the PSD of the DAC is calculated by

$$PSD_{DAC}(s) = |G_{DAC}(s)|^2 \frac{\sigma_{DACq}^2 + \sigma_{DACe}^2}{f_N}. \quad (10)$$

Fast steering mirror

The actuator of the tip-tilt compensation system is a FSM moving an 1" mirror. It provides a mechanical angular range of ± 26.2 mrad and a 3 dB bandwidth of 510 Hz. The dynamic response of the mirror is shown in Figure 3. Both axes are approximated by the same third order transfer function with a structure of

$$G_{FSM}(s) = \frac{b_2 s^2 + b_1 s + b_0}{s^3 + a_2 s^2 + a_1 s + a_0} e^{-sT_{t,FSM}} \quad (11)$$

where b_i and a_i are the coefficients and $T_{t,FSM}$ is an additional dead time. The parameters of the fitted transfer function are shown in Table 1.

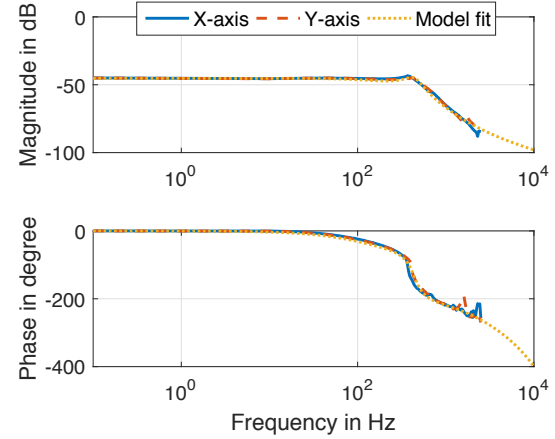


FIGURE 3. Bode diagram of both axes of the PID controlled FSM (OIM101). Similar dynamic behavior is observed for both axes, allowing the utilization of one fitted transfer function for both axes.

The FSM is equipped with a angular, optical position sensor used for the internal controller. Its output signal is used as reference for the FSM whose noise behavior is modeled as a combination of 1/f-noise and white noise. As the noise of this position sensor is acting onto the FSM output angle via the FSM itself, the FSM's dynamics are included in its PSD. The PSD of the FSM is given by

$$PSD_{FSM}(s) = |G_{FSM}(s)|^2 \left| \frac{s + 2\pi \cdot f_{FSMe}}{s + 2\pi \cdot f_{FSMs}} \right|^2 \frac{\sigma_{FSM}^2}{f_N}, \quad (12)$$

where G_{FSM} is the fitted transfer function of the FSM, $\sigma_{FSM}^2 = 0.748$ mV its RMS noise and $f_{FSMs} = 0.1$ Hz and $f_{FSMe} = 1$ Hz the frequencies specifying 1/f noise.

TABLE 1. Parameters of the fitted transfer function valid for both axes of the FSM.

Parameter	Value
b_2	-0.758
b_1	1.041e4
b_0	3.948e7
a_2	2.140e3
a_1	8.762e6
a_0	7.279e9
$T_{t,FSM}$	-40e-6

Tip-tilt sensor

The tip-tilt distortions are measured by means of a custom made QPD circuit. This type of detector (Figure 4) allows measurement of the displacement of a laser spot on the detector area. Its outputs are given by

$$\begin{aligned} x &= \frac{(I_2 + I_4) - (I_1 + I_3)}{I_1 + I_2 + I_3 + I_4} \\ y &= \frac{(I_1 + I_2) - (I_3 + I_4)}{I_1 + I_2 + I_3 + I_4}, \end{aligned} \quad (13)$$

where I_i are the currents delivered by the individual segments [8]. These currents are transformed to voltages by transimpedance amplifiers (TIAs) and the summation and subtraction operations are performed by operational amplifiers. The dynamic behavior is modeled with a combination of a first order low-pass filter for the photo-detector and TIA and a second order low-pass filter for the summing/subtracting amplifiers.

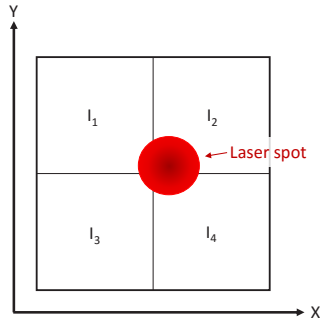


FIGURE 4. Schematic drawing of a QPD. The incoming light beam as well as the output currents I_i are indicated. The position of the laser spot is calculated by Equation (13).

The frequency behavior of the TIA is mainly defined by the feedback resistance R_f and the junction capacitance C_j of the photo-detector. Its 3-dB cutoff frequency is calculated with

$$f_{3dB,TIA} = \sqrt{\frac{GBW}{4\pi R_f C_j}}. \quad (14)$$

The summing and subtracting amplifiers have integrated second order low-pass filters with a 3-dB cutoff frequency of 8 kHz. The dynamics of the QPD are given by

$$G_{QPD}(s) = k_{QPD} \cdot G_{TIA}(s) \cdot G_{SUM/SUB}(s), \quad (15)$$

where G_{TIA} and $G_{SUM/SUB}$ are the transfer functions of the TIA and the summing/subtracting

amplifiers and k_{QPD} includes all gains from the amplifiers as well as the sensitivity of the QPD. This factor is measured to $k_{QPD} = 788$. To include low frequency noise a combination of 1/f-noise with a corner frequency $f_{1/f,QPD}$ and white noise is used. The corner frequency is identified by measurement to $f_{1/f,QPD} = 1$ Hz. The resulting PSD is given by

$$PSD_{QPD}(s) = |G_{QPD}(s)|^2 \cdot \left| \frac{s + 2\pi \cdot f_{1/f,QPD}}{s} \right|^2 \frac{\sigma_{FSM}^2}{F_N}. \quad (16)$$

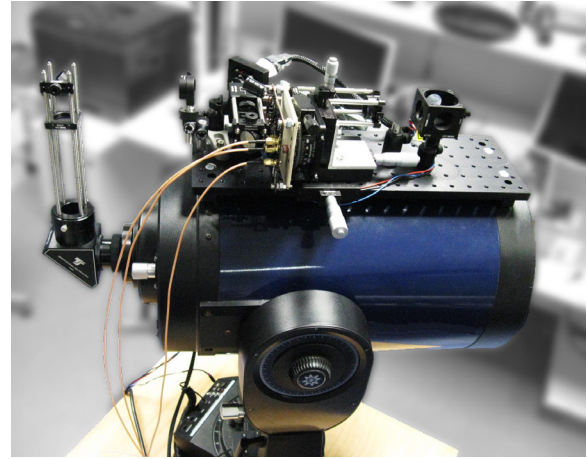


FIGURE 5. Picture of the evaluated system showing the 10" telescope, the workbench and the optical setup.

Optical setup

The optical setup based on a 10" Schmidt-Cassegrain telescope is used to expand the diameter of the laser beam and to reduce the output angular range. The optical setup is mounted on top of the telescope, allowing to freely move the telescope with the original mount. The system is set up as reflective optical communication setup. Using a retro-reflector as measurement target the beam path is identical for outgoing and incoming beam. A beam-splitter separates the two beams and directs the incoming, reflected beam onto the QPD. The output angle is reduced by a factor of $k_{OS} = 4.82$. To take the time delay due to the traveling time of the light into account, which is especially important for long measurement distances, a transfer function of the optical setup is defined using a dead time in the form of

$$G_{OS}(s) = k_{OS} e^{-s \frac{L}{c_0}}, \quad (17)$$

with c_0 quantifying the speed of light and L the single way measurement distance. As the light propagates two times through the optical setup a second transfer function for the reflected light beam is defined by

$$G_{OS^{-1}}(s) = \frac{1}{k_{OS}} e^{-s \frac{L}{c_0}}. \quad (18)$$

SIMULATION

With the LTI system models and the estimated PSDs from the previous section the performance of the tip-tilt correction system is analyzed and evaluated in terms of limiting precision. After derivation of the transfer functions (Equations (19) - (23)) for all considered disturbance sources the total PSD of the system is calculated with Equation (3).

Transfer functions

Using the schematic representation shown in Figure 2 of the compensation system the transfer functions (sensitivity functions) of all disturbances w_i to the output α are derived. Using the notation $G_j(s) = G_j$ the corresponding transfer functions are given by

$$T_{\alpha,FSM} = \frac{G_{OS}}{1 + G_{OS^{-1}}G_{QPD}G_{ADC}CG_{DAC}G_{FSM}G_{OS}} \quad (19)$$

$$T_{\alpha,DAC} = G_{FSM}T_{\alpha,FSM} \quad (20)$$

$$T_{\alpha,ADC} = -CG_{DAC}T_{\alpha,DAC} \quad (21)$$

$$T_{\alpha,QPD} = G_{ADC}T_{\alpha,ADC} \quad (22)$$

$$T_{\alpha,vib} = \frac{1}{1 + G_{OS^{-1}}G_{QPD}G_{ADC}CG_{DAC}G_{FSM}G_{OS}}. \quad (23)$$

With those transfer functions and the models and PSDs from the previous section the individual contributions of each disturbance source to the total pointing error is calculated.

Simulation results

To evaluate the system's performance in terms of angular precision the derived component models, the disturbance sources' PSDs and the individual transfer functions given by Equations (19) - (23) are used to calculate the resulting PSDs for each component at the output α of the system

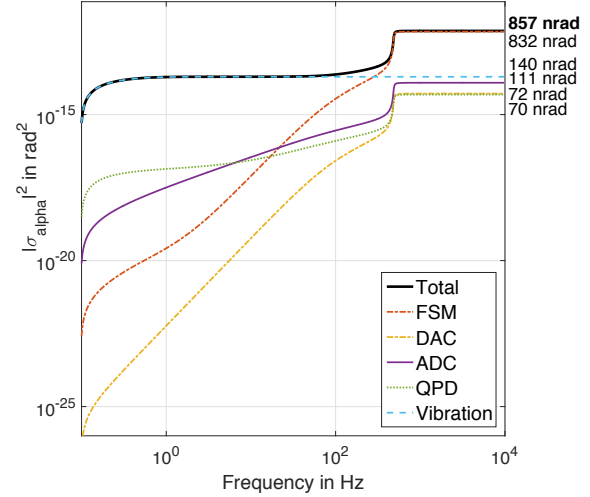


FIGURE 6. Cumulative Power Spectrum of the tip-tilt compensation system determined by Dynamic Error Budgeting.

with Equation (2). The total PSD defining the angular precision of the system is then calculated by Equation (3). Figure 6 shows the CPS of the simulated system, calculated by Equation (4). It indicates the FSM as major noise source and estimates a total angular error of 857 nrad.

EXPERIMENTAL VALIDATION

To verify the simulation results and assumptions from made in previous sections the described tip-tilt compensation system is used. The retro-reflector is actuated by the shaker with a known, pseudo-random pattern introducing reproducible disturbance to the system. The system is operated in open loop and in closed loop mode and the performance is compared to the simulation results. The angular acutation range of the FSM was limited to less than 20% in order to keep the laser beam within the QPD's active area. For further increase of the system's performance the utilization of a different FSM providing less actuation range (e.g. a piezo driven FSM) would be beneficial. Figure 7 shows the angular RMS error of the horizontal axis of the tip-tilt compensation setup, in good agreement with the simulation results. The DC-component of the recorded signal without enabled control is removed to allow direct comparison of both signals. With respect to no feedback control, enabled feedback control allows a reduction of the RMS error from 8.1 μ rad RMS to 1.2 μ rad RMS on the vertical axis and from 5.5 μ rad RMS to 1.0 μ rad RMS on the horizontal axis.

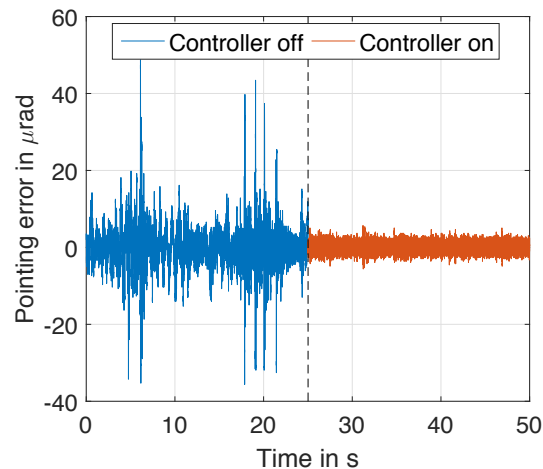


FIGURE 7. Measured angular error of the vertical axis without enabled feedback control. At $t = 25$ s feedback control is enabled. The DC-component of the recorded signal without enabled control was removed for comparison.

CONCLUSION

This paper demonstrates the utilization of Dynamic Error Budgeting (DEB) for systematic analysis of an optical tip-tilt vibration compensation setup. The contributions of all components to the total angular error are described and evaluated by their linear-time-invariant models and the corresponding Power Spectral Densities (PSDs). A feedback controller, designed on base the simulation results, is used for closed loop operation. The simulation results are experimentally verified, successfully demonstrating a reduction of the angular RMS error by a factor of more than 5. Due to similarities of the optical setup with astronomical measurement setups, the evaluated setup may be used to compensate tip-tilt disturbances caused by atmospheric turbulences as well.

ACKNOWLEDGMENTS

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EXTREME STABILITY OF A V-SHAPED CAVITY OPTICAL FEEDBACK LASER SOURCE

Nicolas JOBERT¹, and Mathieu CASADO^{2,3}, Samir KASSI³, Amaelle LANDAIS²,
and Tim STOLTMANN³

¹Mechanical Engineering Department
SYNCHROTRON SOLEIL
Gif-sur-Yvette, FRANCE

²Laboratoire des Sciences du Climat et de l'Environnement
UMR 8212, CEA-CNRS-UVSQ-UPS
Gif-sur-Yvette, France

³Laboratoire Interdisciplinaire de Physique
Université Grenoble Alpes / CNRS
Grenoble, France

MOTIVATION

Trace detection created a bridge between geochemistry and climate sciences. For instance, geochemical tracers such as H and O isotopes in the water molecules can be used as a paleoclimate temperature proxy from ice cores, as an atmospheric tracer in the vapour and in the precipitation, or as a tracer throughout the entire hydrological cycle. Water stable isotopic composition surveys (i.e. concentrations in $H_2^{18}O$, $H_2^{17}O$, and $HD^{16}O$) are now often conducted via Infrared (IR) optical spectroscopy ([1] Casado 2016). One key aspect for the successful determination of isotopic concentrations is the availability of an extremely stable a laser source, allowing the accurate measurement of absorption peaks, whose width spans several *GHz*. For each absorption peak, the concentration of each species is related to the surface under the corresponding line shape, and not only the peak height. Hence, it is needed to have a spectral resolution (and stability) of the order of a *kHz*, i.e. much better than available tunable laser sources linewidth.

Optical resonators are commonly used in infrared spectroscopy to provide frequency etalons. Indeed, in such cavities, the light can only build up for frequencies such as the light beam fold back on itself, which are determined by the length of the cavity. In order to obtain the best frequency stability, it is important that the length of the cavity remains constant, both at short and long time scales: at short time scale (typically, vibrational time scale), the variations of length of the cavity induce a larger spectral width of the laser light; at long time scale

(typically, dilatation processes), the variations of length of the cavity induce a drift of the frequency of the laser. Here, the linewidth and the drift of a rather noisy laser source (a distributed feedback diode, DFB) are reduced by means of a high-finesse V-shaped optical cavity ([2] Burkart et al., 2014). Once the DFB laser is locked in this cavity, its frequency stability is only limited by the V-shaped optical cavity dimensional stability.

Here, the main purpose is to build an infrared spectrometer for trace detection purposes. In order to obtain the *1kHz* needed precision and accuracy, we need to be able to lock on absorption peak of molecules at regular interval, which requires a drift lower than *1 MHz* at the daily scale. Yet, we want to evaluate how far can the optical feedback technique be pushed compared to the best available cavities: the one used to lock atomic clocks which yields stability up to 10^{-18} combining a ultrastable linear cavity with an absolute frequency reference from atomic transitions ([3] Hinkley et al., 2013). Indeed, this is an interesting alternative method thanks to its intrinsic optical feedback properties to stabilise a laser. In this manuscript, we will compare the present performances of the cavity with the expectations from the design and the numerical resolution compared these two goals.

SYSTEM OVERVIEW

This manuscript deals with the 3rd generation of such cavity: this system is composed of a monobloc Zerodur cavity, shielded by a series of

enclosures and actively controlled against thermal and vibrational disturbances.

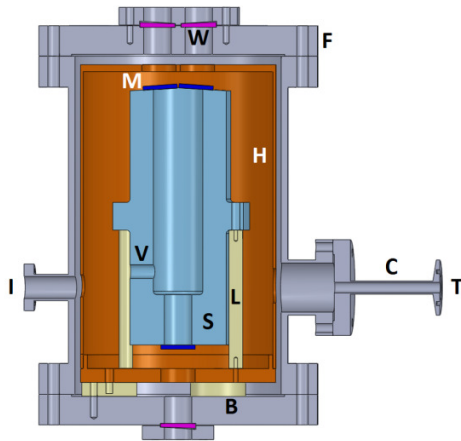


FIGURE 1. Cavity and enclosures overview

Currently, the system generates a single frequency light beam at 215 THz with a linewidth estimated below 100Hz and a drift rate from 1 to 3 Hz.s⁻¹, hence yielding for deformation rates about 10⁻¹⁴ s⁻¹. These estimates are at the detection limit of the frequency measurement system, yet, we expect mechanical constraints (vibration and thermal mechanical stresses in particular) to contribute to frequency noise of light building up in the cavity.

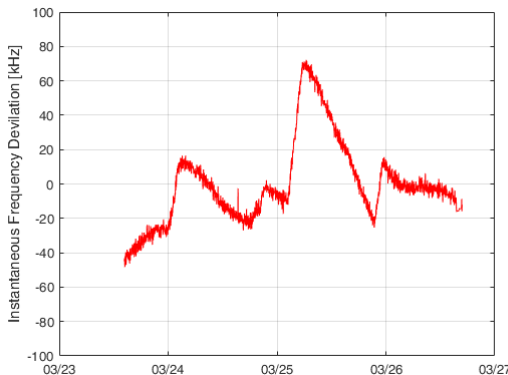


FIGURE 2. Cavity Instantaneous Frequency

Figure 2 presents the frequency variations during 92 hours and shows various behaviors: states during which the frequency is globally stable (rates below 1 Hz.s⁻¹) and which can last up to 12 hours, can abruptly change into a state during which a monotonous and important drift is observed (up to 3 Hz.s⁻¹). In both cases, short variations of several kHz are observed. If the latest is most likely attributed to vibrations (impact in the environment of the cavity), the

other behaviors are not obviously linked with the temperature variations measured over the flange in which is enclosed the cavity. Still, considering how small the effects are (a few pm.s⁻¹ of variations of length of the cavity), we want to evaluate whether a thermo-mechanical approach can explain the frequency drift.

In this manuscript, we will review an ensemble of processes which could, one way or the other, impact the length of the cavity, and thus the frequency of the light, at different time scale from the millisecond to several days. We first evaluate the order of magnitude of the expected impact for each process in order to select the most likely candidates. Then we use Finite Element Methods (FEM) to quantify the impact of each of the selected processes. Because the system is in a confined environment, the sensitivity to vibrations and temperature is rather limited. In order to validate the thermo-mechanical models developed here, we realize several set of stimulated experiments during which we generate vibrational and thermal perturbations. The list of processes we consider includes global and spatially distributed variations of temperature, vibrations either from the acceleration of the table or from acoustic waves, variations of the gravity field, heating from the light accumulating inside the cavity or the dissipation of mechanical vibrations, effect of the pressure variations in the vacuum chamber (including both the change of the refractive index of the vacuum and the adiabatic compression), creeping of the Zerodur glass, radiation pressure on the mirrors, surges in ion pump temperature...

METHODS

Experimental-Based Investigations

Experimentally, all investigations involve the utilization of a means to track the evolution of the optical cavity instantaneous resonant frequency, as well as sensors to capture the external disturbance sources.

Instantaneous Optical Resonant Frequency Tracking

The variations of the length of the optical cavity are tracked by monitoring the frequency variations of the light of the laser locked on the cavity. Indeed, because of the high reflectivity of the mirrors, the light can only build up inside the cavity for frequencies such as the light fold back

on itself (constructive interferences). For a V-shaped cavity, this happens when

$$\nu_m = m \frac{c}{4L} \quad (1)$$

where m is an integer, c is the celerity of light and L the distance between two mirrors. Thus, by monitoring the frequency of the laser, we obtain directly information on the dilatation of the cavity. We used a self-referenced optical frequency comb (Toptica FFS 1550) as a frequency reference to monitor the variations of the frequency of the laser locked on the cavity. The beatnote of the light of the comb with the laser locked on the cavity provide an estimation of the difference between the cavity mode and the optical frequency of the comb. This method provides an absolute estimate of the frequency of the cavity but is not precise enough at short time range (below one minute) to detect the very small frequency variations of the cavity. For high resolution cases, we use a temperature-stabilized low finesse cavity as a frequency etalon. As the modes of this second cavity are rather large (76 kHz), by injecting the laser light on the edge of a mode, we obtain a locally linear slope between the cavity transmission and the frequency. This method provides with the frequency variations of the VCOF in respect with the low finesse cavity. Yet, the low finesse cavity can drift and thus, this method is not accurate for time scales above several minutes.

External Sources Disturbances Characterization

Thermal effects: An array of Pt1000 sensors have been used to capture the thermal fluctuations at the boundaries of the system, namely the Vacuum Chamber. Each probe is individually fitted with its own bridge and DAQ, completely enclosed within the enclosure, so that the measurement system itself operates in a stable environment. Each probe nominal value has been pre-adjusted so as to minimize the sensor to ensemble average deviation.

This allows capturing of the time-histories of temperature of various parts of the vacuum chamber, which will propagate, through conduction and radiation, into the cavity.

The total noise of the system was evaluated to be about 0.5 mK RMS ([0-1440cycles/day]).

Vibrational effects:

By design the Zerodur cavity is a compact and rigid bloc mounted on a series of comparatively flexible Macor legs, which will therefore amplify the externally transmitted motions. In order to

assess the amplitude of those motions, it is necessary to capture the acceleration levels on the Vacuum Chamber.

The choice of the sensor was not straightforward, since a number of conflicting considerations come into play, the most stringent being: the larger the sensor, the lower the noise floor but conversely, the heavier the sensor, the narrower the bandwidth. In our case, stringent limitations were placed on sensors dimensions, so we selected the largest possible sensor, which occurred to be a DJB A121 IEPE accelerometer, with a bandwidth of 2kHz, and a noise floor below 1mm/s^2 ([10-2kHz]), hence a noise linear spectral density about $20\mu\text{m/s}^2/\text{Hz}^{1/2}$. Assuming the cavity has mechanical resonances above 100Hz, and modal damping ratio of about 0,5%, this corresponds to a detection threshold of about 2.5mm/s^2 (@100Hz), and a corresponding amplitude of 6nm RMS.

Numerically-Based Investigations

The approach encompassed the development of static and dynamic models of the cavity/shield system so as to compare the severity of both thermal and vibration induced fluctuations. All analyses have been prepared and conducted using ANSYS rev. 14.5 solver.

Thermal Structural Analyses

A thermal structural FE model of the entire setup has been developed, in order to capture temperature propagation in the Vacuum Chamber, Inner Shield, Cavity and Mirrors. Both conduction and radiation are accounted for, as well as thermal inertia effects.

Using this model, it is possible asses the response of the entire system to external and internal heat sources, namely:

- Temperature fluctuations as recorded at the outer surface of key parts of the Vacuum Chamber
- Local heat load due to laser beam absorption by mirrors

Structural Analyses

A structural model of the setup has been developed in order to capture various effects:

- Static loads: inertia forces (gravity loads), radiation pressure.
- Dynamic loads: vibrations of the Vacuum Chamber

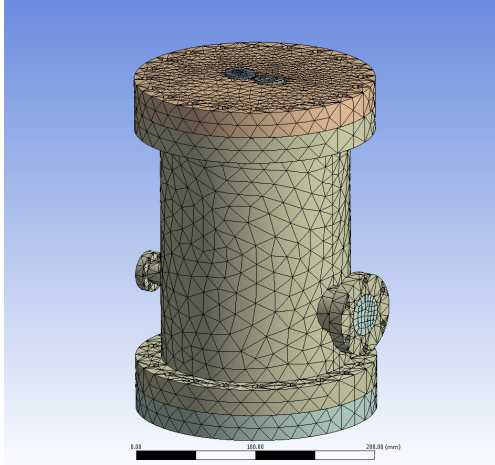


FIGURE 3a. Global FEM

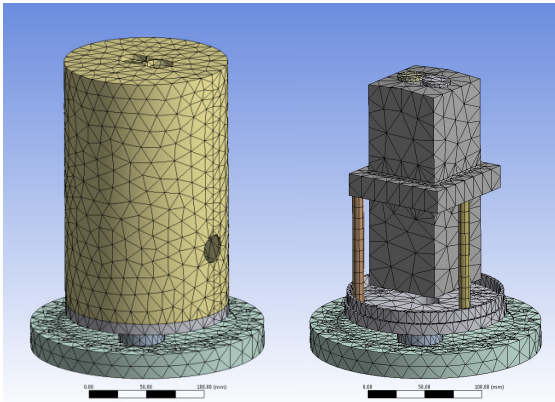


FIGURE 3b. Local FEM (with and w/o IS)

RESULTS

Numerical evaluation of the performances

Thermal effects:

The estimation of frequency variation due to thermal mechanical effects is performed in a two phase process:

1. Propagate temperature fluctuations from the outer boundary (i.e. the vacuum chamber external surface) of the system shown in Figure 3 to the cavity
2. Estimate induced dilatations, assuming a known value of the instantaneous Coefficient of Thermal Expansion (CTE) for Zerodur.

Temperature response is estimated either in the frequency domain (using Transfer Function) or in the Time Domain (using Impulse Response). Both representations are insightful and are given hereafter, the former allows reading the

attenuation ratio as a function of the duration of thermal cycles, and the latter giving access to the actual response time-history (i.e. complete waveform).

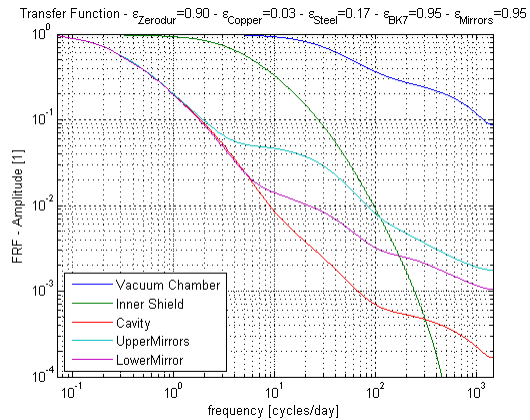


FIGURE 4. Thermal Transfer Function

In our case, the FEM indicates that the diurnal cycle temperature fluctuations (i.e. 1 cycle/day) are reduced by a factor of 5.

Once the dilatation ratio is obtained, the frequency deviation can be simply estimated using $\frac{\Delta\nu}{\nu} = -\text{CTE} \cdot \Delta T$.

Using the temperature time-history recorded simultaneously with frequency deviation displayed on Figure 2, the response temperature and predicted frequency deviation (assuming an envelope value of 10ppb/K for CTE of Zerodur) are given on Figure 5 hereafter.

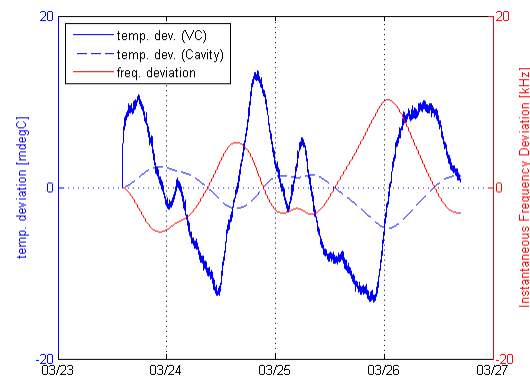


FIGURE 5 – Temperature and Optical Frequency Deviation

On the one hand, it is reassuring to note that the reduction factor of 5 as read on the Transfer Function applies to the time domain method.

On the other hand, even using an envelope value for the CTE of Zerodur, the predicted maximum frequency deviation is about 15kHz, i.e.6 times *lower* than that given by the optical comb. Hence, thermal effects on the Cavity are not sufficient to explain the observed drifts.

Additional investigations including mirrors contribution might be useful and are considered for the future.

Dynamic effects:

The contribution of dynamic effects is estimated by computing the Frequency Response Function of the Cavity, i.e. a dynamic model as shown of Figure 3b. Firstly, the eigenmodes of the system are estimated. Secondly, there are used in a mode-superposition procedure to apply a unitary acceleration (inertia) load. Lastly, this Frequency Response Function is multiplied by the actual (measured) acceleration spectrum as obtained on the outer surface of the Vacuum Chamber.

In our case, the Cavity behaves as a rigid body with horizontal modes around 110Hz, vertical mode about 1100Hz. Additionally, two local modes corresponding to flexure of upper mirrors (which are in contact with the cavity on roughly half of their circumference) are obtained at about 6000Hz.

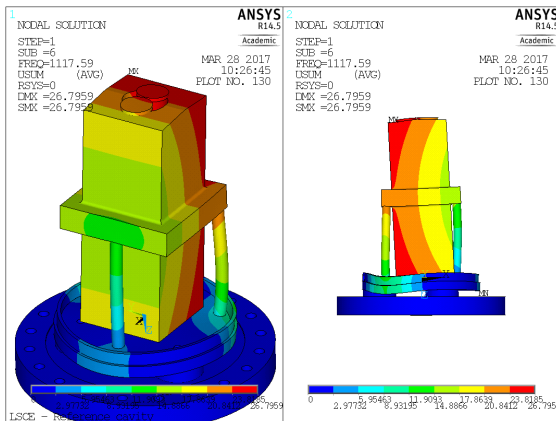


FIGURE 6a – Global Cavity Mode

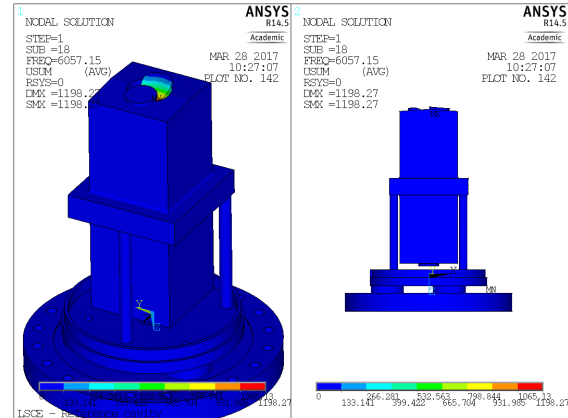


FIGURE 6b – Local Mirror Mode

The relative motion of the Lower and upper mirrors are calculated and added to yield the total sensibility of cavity (unit: $\text{pm}/(\text{mm}/\text{s}^2)$), as show hereafter.

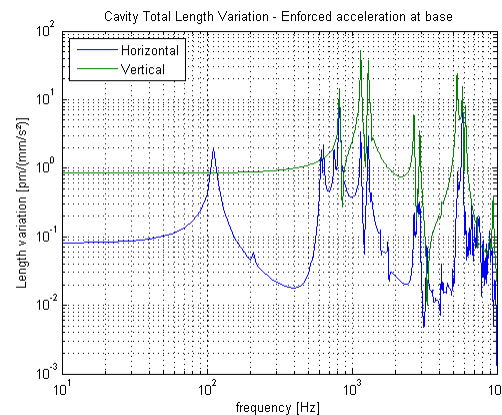


FIGURE 7 – Cavity Length Acceleration FRF

Overall, the cavity sensitivity is – as expected - larger for vertical solicitations compared to vertical directions. Outside from resonances, its value if about $1\text{pm}/(\text{mm}/\text{s}^2)$. As it is difficult to obtain an environment with acceleration levels lower than $0.1\text{mm}/\text{s}^2$, it can be expected that mechanical vibrations will create an optical frequency noise floor of about 100Hz.

In our case, we used two different recordings of environmental vibrations, one in very busy, and the other in completely quiet conditions.

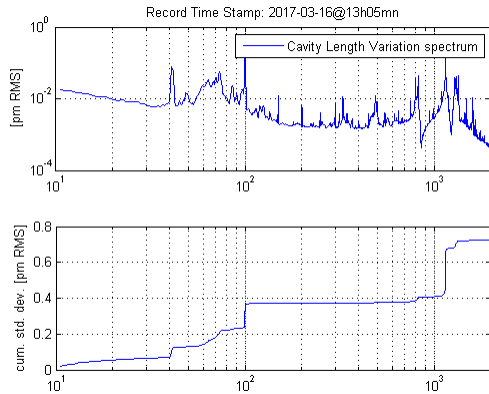


FIGURE 8 – Cavity Length Variation (Vibrations)
The maximum contribution comes from the excitation peak close to 100Hz, and from the amplified response close to 1100Hz. Overall level is about 0.8pm RMS, hence an optical frequency noise contribution of about 0.4kHz, compatible with the “white noise” seen on figure 2.

Other Effects:

A number of other, presumably much less significant effects can be taken into account, namely:

1. Tilt effects (gravity)
2. IR residual radiosity (due to variation of external temp)
3. Radiation Pressure
4. Laser power instabilities
5. Conversion of mechanical vibrations into dissipated heat load (friction)
6. Imperfect vacuum leading to changes in refractive index
7. Adiabatic compression of residual gases
8. Ionic Pump power burst
9. Long term Flow of Zerodur

Not surprisingly, none of those sources yielded significant contribution, all remaining well below 1ppt length variation, the only exception being the source #5, which reached about 10ppt length variation. However, it was later verified that the vibrations levels are quite steady, hence no sharp rise in temperature could be created.

Experimental evaluation of the performances

We combine the results of both frequency measurement methods: the beatnote with the optical frequency comb for low resolution and the estimation through the cavity transmission

for low resolution. In Fig. 9 are presented these results compared to the different estimations.

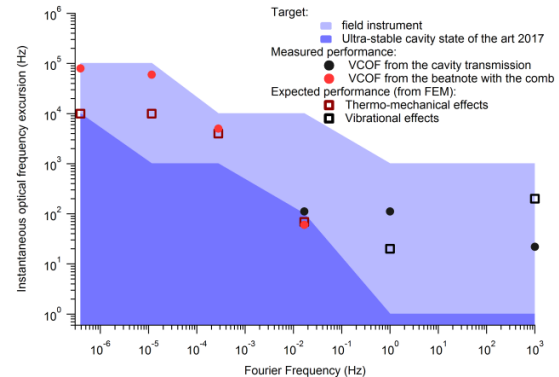


FIGURE 9 – Optical Frequencies Summarized

CONCLUSION

A number of investigations have been conducted, combining recorded signals of the environmental factors (temperatures, vibrations) and numerical models in order to estimate their consequences in terms of optical frequency deviations. Overall, measured values of frequency noise levels are largely above those expected, hence either:

1. the models fail to capture the most relevant effects, or
2. the VCOF is actually more stable than indicated by our current monitoring systems (optical comb / etalon transmission)

To discard hypothesis #1, some controlled excitations tests were realized, in order to consider effects in isolation. To discard hypothesis #2, some additional tests are scheduled in order to separate frequency variations from optical comb and VCOF.

Clearly, the current version of the VCOF is well acceptable for a spectrometry usage. Indeed, it could even be envisioned to use it as an alternative for stable optical source in an atomic clock. Overall, the major hurdle so far lies in the lack of an optical frequency tracking tool. That would be simultaneously fulfilling the (stringent) demands of being:

- accurate (kHz level) and reliable for long-term ($>10^5$ s) phenomena, while still
- precise enough (Hz level) at short (10^{-3} to 1s) time scales to capture vibration transients

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ANGULAR MEASUREMENT TO 0.1 ARC SECONDS: GAITHERSBURG LATITUDE OBSERVATORY CIRCA 1900

Richard L. Rhorer
Rhorer Precision Engineering LLC
Gaithersburg, Maryland, USA.

INTRODUCTION

In 1899 the Gaithersburg Latitude Observatory was constructed and started taking nightly measurements of the positions of designated stars. The goal of these measurements was to determine the magnitude of polar motion, or “wobble”, as the Earth rotates. A change in the pointing direction of the Earth’s pole, over time, causes an apparent motion in a star’s location with respect to a fixed point on the earth. This change in polar direction also causes a corresponding change of the indicated latitude of the observer. The polar motion is a very small amount and even though it can be predicted by theoretical models, the actual measurement of this motion presents a significant precision metrology challenge.

The Earth is not a perfect sphere and the rotation axis varies slightly tracing a small circle, rather than a single point, in space. In about 1750, based on classical-mechanics calculations by the famous mathematician Leonhard Euler (1707-1783), this polar motion was predicted to have a period of about 10 months and a magnitude of less than one arc second (a wobble of 1.0 arc second would trace a circle of about 30 m diameter at the Earth’s pole.) This small change in the pole direction was difficult to measure with existing instruments, but an American astronomer, Seth Carlo Chandler, Jr., developed a special telescope system and in 1891 he was able to make very precise measurements of star locations. He found that some discrepancies in his measurements could only be explained by a change in the latitude of his observatory at Harvard. After discussions at international conferences, a plan was developed to make identical star observations at different positions around the Earth in an attempt to quantify experimentally this polar motion. This plan involved many scientists and a great deal of international cooperation—funding was obtained and work started by the late 1890s. [1]

The observatory in Gaithersburg, Maryland, USA, was one of five observatories spaced

around the Earth at very nearly the same northern latitude of 39 degrees 8 arc-minutes (39° 08’ North) all making observations of the same stars. The observatories in Ukiah, California; Tschardjui, Turkmenistan; Carloforte, Italy; Misuzawa, Japan; and Gaithersburg were all part of an international project called the International Latitude Service (ILS). Within a few years observatories in Cincinnati, Ohio and Kitab, Uzbekistan were added. The data from all the observatories was sent to the Misuzawa observatory for data reduction.

The program was originally planned to last five years, but the observatories continued in operation until late in the 20th Century—the Gaithersburg Observatory was operated until 1982 (with a pause from 1914 to 1932). The observatory building and meridian-alignment pier have been preserved as a Gaithersburg City Park and a National Historic Landmark of the National Park Service.



FIGURE 1. Gaithersburg Latitude Observatory.

The observatory building, shown in Figure 1, is built with a roof that splits open by using a rope and pulley system. The visual zenith telescope (VZT) instrument could look straight up through the roof opening and it could be rotated horizontally to look through a side opening in the building toward the meridian pier. The pagoda-type structure housing the pier is shown in Figure 2. It has two small window openings for aligning the telescope accurately to the north-

south direction from both sides of the telescope mount.



FIGURE 2. *Pagoda structure housing the meridian alignment pier (note: why are there two windows?).*

The telescope is no longer at the Gaithersburg observatory. It has been preserved in a National Oceanic and Atmospheric Administration (NOAA) museum in Northern Virginia. [2]

During the years that the observatories were in service over 66,000 operational nights were recorded. Using data from the ILS observatories their mean latitudes were calculated within 0.001 arc second; the Gaithersburg observatory location was specified as $39^{\circ} 8' 13''.202$. [3]

One can encounter several different technical and scientific disciplines in studying the Gaithersburg Latitude Observatory (GLO): geodesy, geophysics, classical mechanics, astronomy, astrometry, navigation, surveying, optical tooling, as well as optical instrument design. Researching the GLO from the perspective of any, or all, of these disciplines can be very interesting; however, this paper looks at the GLO from the instrument maker's standpoint—what were the challenges and requirements presented to the person, or team, that made the observatory's VZT?

It is beyond the scope of this paper, but the history of the latitude observatories that may make the most lasting contribution is the approach toward international cooperation

demonstrated by the scientists that initiated the effort in the late 19th Century.

THE MEASUREMENT CHALLENGE

To determine the wobble or polar motion as the earth turns on its axis, accurately measuring the position of a particular star, or group of specified stars, over a period of several years is the technical challenge. The measurements needed to be reproducible by different observers at different observatories and the instruments had to be stable over periods of many years.

The measurement problem is complicated by the fact that the stars move rapidly as the earth turns (daily cycle) and also there is a very long period change in the star locations commonly called the *precession of the equinoxes* (period of about 26,000 years). The small polar motion that the researchers of the ILS were looking for has to be seen on top of these motions. In the 18th Century the component of this polar motion of interest was predicted to have a period of less than one year (about 10 months) and a magnitude of less than an arc second.

The precession of the equinoxes was understood and well characterized before the 1800s, first discovered in about 100 BCE by an astronomer named Hipparchus. [4] There is a long history of astronomers measuring and recording the positions of the stars, taking into account the precession, as was required for the ILS.

In some literature related to the history of the latitude observatories the terms *precession* and *nutation* are used referring to changes in location of the earth's axis of rotation. The measurements required by the ILS might be thought of as an extension of the traditional star locations recorded by astronomers to produce nautical almanacs (although higher precision was being sought by ILS). A valuable reference for both the history and practical application of navigation is *The American Practical Navigator* originally written by Nathaniel Bowditch (1773-1838), but updated and regularly published by the U.S. Navy in modern times. Definitions from Bowditch:

Precession is "Change in the direction of the axis of rotation of a spinning body, as a gyroscope, when acted upon by a torque." And *precession of the equinoxes* is defined as "The conical motion of the earth's axis about the

vertical to the plane of the ecliptic, caused by the attractive force of the sun, moon, and other planets on the equatorial protuberance of the earth." *Nutation* is defined as "Irregularities in the precessional motion of the equinoxes". [5]

In references on the mechanics of gyroscopes the terms precession and nutation are used to describe angular motions of the spinning axis about two different orthogonal axes (slightly different than the language used in navigation.) Apparently, to avoid confusion the term *polar motion* is commonly used as an all-inclusive term instead of precession and nutation in modern earth rotation studies. [6]

To make the measurements at the ILS observatories like Gaithersburg, a series of specific stars were specified. The observer was provided the position of each star (declination and sidereal hour angle, or right ascension) that took into account the precession and the exact time. The observer then recorded accurate readings of the altitude of the specific star. This approach of specifying the stars apparently worked well:

"The last observer at Gaithersburg, Mac Currin, told the Gazette, "It was good to watch the stars. I find it incredible that all of these star pairs were picked and lined up to observe, and each night week after week, month after month, year after year, at just the right moment, those stars would pop into view." Currin conducted observations in Gaithersburg for 25 years. In 1982, human observers were replaced by satellites and the latitude observatories went into retirement." [7]

The ILS observatories relied on astronomical almanacs. Apparently, very accurate star location data were available by the 19th century, for example shown in Figure 3, with numbers reported to less than arc second value. This example was copied from a history chapter of Bowditch which notes that the correction factors given in the table "can be used to obtain reasonably correct values today."

The astronomical data supplied to the ILS observers were apparently very reliable which allowed them to obtain over 770,000 observations during the life of the program (1899 to 1982). In the ILS measurements they were looking for precision pushed beyond what was normally obtained in astronomical observatories. One of the driving arguments for the ILS project

initially was that some astronomers had observed variations in their data of star locations that might be caused by a polar motion not previously characterized. Also, the latitude observatories contribution could impact very precise navigation and accurate land surveying that relied on star sightings to determine latitude.

FIXED STARS, 1855.

MEAN PLACES OF 100 PRINCIPAL FIXED STARS, FOR
JANUARY 1, 1855.

Star's Name.	Mag.	Right Ascension.	An. Variation.	Declination.	An. Variation.
α ANDROMEDA	2	0 0 53.97	+ 3.087	+28 17 23.3	+19.93
γ PEGASI (<i>Algenib</i>)	3.2	0 5 46.37	3.085	+14 22 38.1	20.05
β HYDRI	3	0 18 3.62	3.292	-78 4 23.1	20.23
α CASSIOPEE	var.	0 32 18.36	3.356	+55 44 29.2	19.83
β CETI	2	0 36 18.45	3.016	-18 47 0.1	19.86
α URS. MIN. (<i>Polaris</i>)	2	1 6 29.82	+18.117	+88 32 11.3	+19.23
δ CETI	3	1 16 46.57	3.000	- 8 55 58.6	18.74
α ERIDANI (<i>Achernar</i>)	1	1 32 18.42	2.238	-57 58 28.2	18.59
α ARIETIS	2	1 59 0.44	3.365	+22 46 28.4	17.29
γ CETI	3.4	2 35 47.42	3.102	2 37 19.4	15.44
α CETI	2.3	2 54 42.21	+ 3.129	+ 3 31 4.7	+14.40
α PERSEI	2	3 13 59.52	4.243	49 20 26.8	19.25
γ TAURI	3	3 38 52.31	3.553	+23 39 11.0	11.54
γ ERIDANI	3	3 51 15.91	2.796	-13 55 26.7	10.59
α TAURI (<i>Aldebaran</i>)	1	4 27 36.26	3.436	+16 12 49.4	7.72
α AURIGÆ (<i>Capella</i>)	1	5 5 59.03	+ 4.423	+45 50 41.8	+ 4.27
β ORIONIS (<i>Rigel</i>)	1	5 7 31.23	2.684	- 8 22 22.5	4.54
β TAURI	2	5 17 7.72	3.791	+28 28 48.3	3.55
δ ORIONIS	2	5 24 36.06	3.066	- 0 24 37.8	3.05
α LEORIS	3	5 26 20.19	2.648	-17 55 46.0	2.94
ϵ ORIONIS	2	5 28 51.43	+ 3.044	- 1 17 54.6	+ 2.71
α COLUMBÆ	2	5 34 24.05	2.177	-34 9 13.3	2.23
α ORIONIS	var.	5 47 19.35	3.249	+ 7 22 32.6	+ 1.11
μ GEMINORUM	3	6 14 11.30	3.636	+22 34 59.9	- 1.37
α ARGUS (<i>Canopus</i>)	1	6 20 44.13	1.330	-52 37 4.7	- 1.81
δ I (<i>Herc.</i>) CEPHEI	5	6 31 6.10	+30.650	+87 15 7.9	- 2.80
α CANIS MAJ. (<i>Sirius</i>)	1	6 38 45.60	2.646	-16 31 12.8	4.52
α CANIS MAJORIS	2.1	6 52 55.69	2.360	-28 46 40.3	4.58
δ GEMINORUM	3.4	7 11 27.65	3.597	+22 14 41.7	6.16
α GEMINOR. (<i>Castor</i>)	2.1	7 25 20.49	3.841	32 12 6.2	7.37
α CAN. MIN. (<i>Procyon</i>)	1	7 31 42.52	+ 3.145	+ 5 35 35.7	- 8.79
β GEMINOR. (<i>Pollux</i>)	1.2	7 36 26.23	3.681	+28 22 19.9	8.26
δ ARGUS	3	8 1 22.22	2.557	-23 53 20.5	10.06
ϵ HYDRÆ	3.4	8 39 5.74	3.189	+ 6 56 52.2	12.86
α URSE MAJORIS	3	8 49 15.44	4.123	+48 36 26.7	13.78
ϵ ARGUS	2	9 13 12.52	+ 1.602	-58 40 3.3	-14.91
α HYDRÆ	2	9 20 27.65	2.951	- 8 1 56.8	15.36
δ URSE MAJORIS	3	9 23 7.85	4.048	+52 20 6.3	16.13
ϵ LEONIS	3	9 37 36.82	3.424	24 26 22.0	16.84
α LEONIS (<i>Regulus</i>)	1.2	10 0 38.72	3.205	+12 40 26.4	17.40
γ ARGUS	2	10 39 26.75	+ 2.306	-58 55 21.5	-18.74

FIGURE 130b.—Star data from the 1855 *Nautical Almanac*. The annual corrections in declination and right ascension can be used to obtain reasonably correct values today.

FIGURE 3. Historical almanac data of 1855 from Bowditch. [4]

In a development often found in the history of science, when new experimental observations force the development of improved mathematical models, there were measurements from the ILS observatories data that indicated a discrepancy when fitting to their polar motion model. The astronomer at the Mizusawa, Japan, ILS Observatory named H. Kimura, became famous for adding an additional

term, called the Kimura Z-term, to allow a better fit to the existing model.

“Seventy years after the discovery of Kimura’s Z-term, a physical interpretation was made by Wako (1970), an astronomer of ILOM. In short, Kimura’s Z-term was due to the inadequacy of the nutation model used to calculate the ILS star places. The adopted nutation table was based on the rigid Earth model. However, the real Earth is composed of an elastic mantle, liquid core, oceans, etc., and therefore the nutation of the real Earth is remarkably different from the rigid Earth nutation. The largest component of this discrepancy appeared as an annual variation of about $0''.02$ (60 cm) amplitude in the Z-term. Although the precision of a single observation by the VZT was about $0''.1$ (3 m), by taking average, such small annual variation could be detected.” [8]

Some of the results of the nightly measurements from the ILS Observatories are shown in Figure 4. This plot, photocopied from Yokoyama [9], shows polar motion varying from about 0.2 arc second to 0.6 arc second and a period of 14 months (about 8.5 periods in 10 years). The dark line is from a fitting technique called “Orlov mean latitudes” which provides a picture of how the earth’s axis varies over many years.

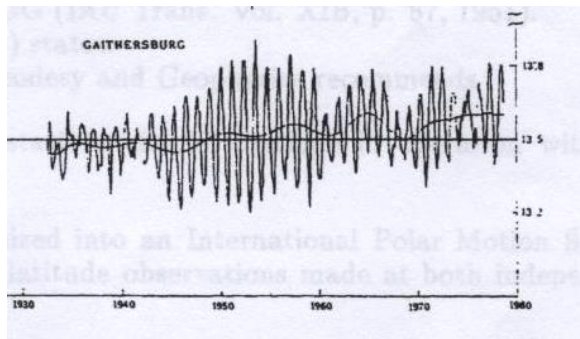


FIGURE 4. Historical data from the GLO. [9]

By the second half of the 20th Century technology advances made it possible to improve, and in a sense, verify the ILS’s more than eighty years of data. One modern approach is based on radio astronomy. An interesting plot of polar motion is shown in Figure 5. This plot is photocopied from a brochure published by the National Radio Astronomy Observatory: “Observations with the Green Bank Interferometer help establish the track of the moving pole (projected as a four

year record in the late 1970s) with respect to its mean location in 1903 (cross at $x, y = 0.0$).” [10]

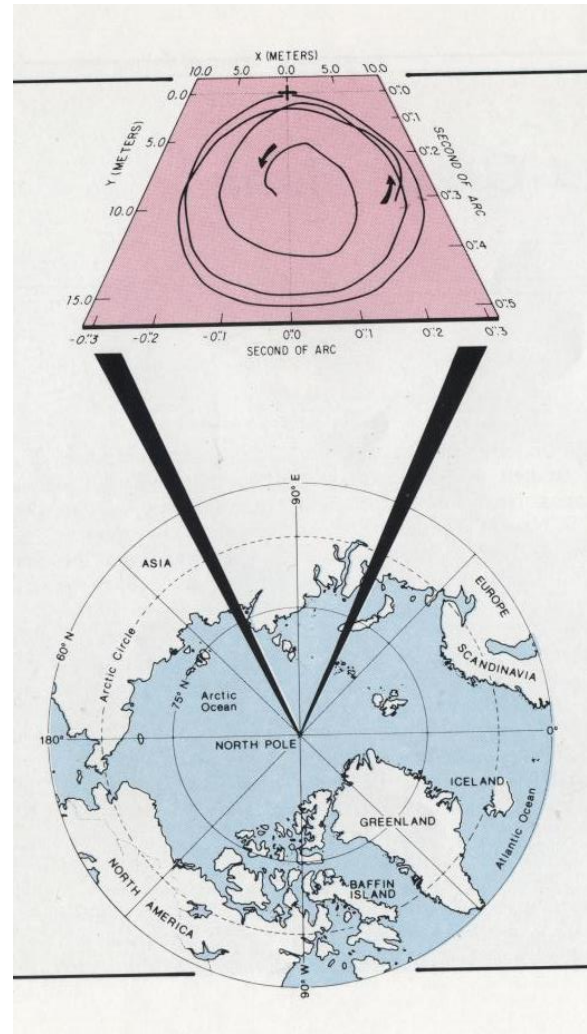


FIGURE 5. Polar Motion plot from measured by radio interferometry in the 1970s. [10]

From the advantage point of 100 years later, we can visualize the problem that the ILS observatories set out to address: “measure the polar motion with a precision less than 0.1 arc seconds.” Of course the scientists, in the 1890s arguing for project support, did not yet know the answer.

MEASUREMENT PROCEDURE

The challenge for the instrument maker of the latitude observatory instruments was to provide a capability to measure sub-arc second angles with a high degree of reproducibility involving several different observers over many years of service. To meet the challenge it appears that the instrument maker employed the state-of-the-

art in optical fabrication, mechanical system design and fabrication, plus an understanding of error minimization schemes.

The instrument design used a rotation/inversion of the telescope and specific star readings. This approach is similar to the method employed by surveyors using an engineer's transit in normal and inverted mode (called "plunge and rotate" by surveyors.) "The measuring of two angles, one measured with the telescope normal and the other measured with it inverted will be free from instrument error." [11]

The historical records of the ILS observatories describe their procedure. Reviewing this procedure tells us a great deal about the instrument. From the display panel at the GLO park:

"Each night, an observer would conduct a series of tasks as they made and recorded their observations; including rotating, aligning, and leveling the telescope, taking numerous reading of stars, weather and other factors. A typical night at the observatory began about an hour before the first reading was taken with the Zenith Telescope (ZT) housed inside the observatory. The roof of the observatory was opened to allow the temperature around the ZT to stabilize."

"The ZT was then brought to a horizontal position and the center crosshair of the telescope was trained on the center crosshair of the Meridian Mark Pier (also known as the Azimuth Marker)—and the position of the telescope read. The ZT was then brought back to a vertical position, rotated 180 degrees about the vertical axis, leveled, and again pointed at the pier—and its position read. This series of direct and reverse readings was used to correct the errors in the adjustment of the optical components in the ZT."

"The ZT eyepiece assembly includes a movable crosshair. As a star passes through the field of view, the observer tracked the path of the star with the crosshair and readings were taken. The readings were recorded and levels attached to the telescope were read and recorded. Then the telescope was rotated 180 degrees on about on its vertical axis, so the eyepiece was on the opposite side of the Pier and all levels were checked. About one minute before the second star of the pair appeared, the levels were read and recorded once again. When the second

star had passed through the field of view, and the micrometer and level readings recorded, observation of star pair number one was concluded." [12]

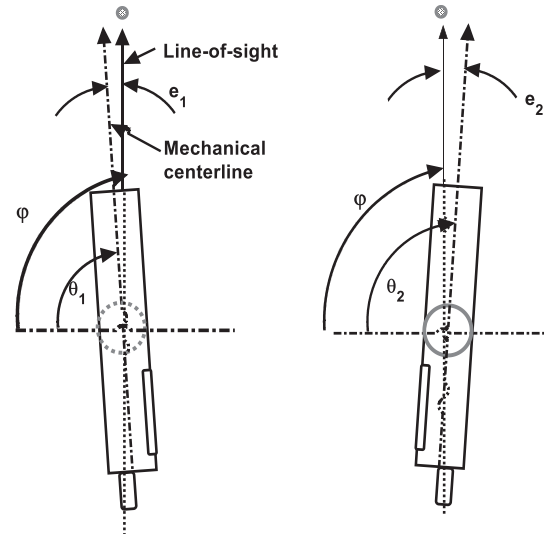


FIGURE 6. Schematic drawing of the ILS telescope as it was set to align on a star from the East and West sides of the telescope mount post.

The method of telescope rotation and inversion is illustrated schematically in Figure 6. When the telescope is in the position of the east side of the mount, shown in the left sketch, and when on the west side of the post is shown in the right sketch. The measurements of the angle of the telescope from the east and west configurations are θ_1 and θ_2 (the "readings"). The differences between the mechanical centerline and the optical line-of-sight (aligned on the star) for the two different configurations are e_1 and e_2 . The true angular location of the star is ϕ . If the average of the two angular readings is taken,

$$\theta_{\text{average}} = \frac{\theta_1 + \theta_2}{2} = \frac{(\phi - e_1) + (\phi + e_2)}{2}$$

and if $e_1 = e_2$ then,

$$\theta_{\text{average}} = \frac{2\phi}{2} = \phi.$$

The misalignment of the mechanical centerline and the actual line-of-sight is eliminated by averaging the two readings. The levels and angular reading scales are represented by the

mechanical centerline and the optical line-of-sight of the telescope is visually aligned to the specified star. This error reduction depends on the difference between the mechanical center line and the actual line-of-sight being the same for both positions ($e_1 = e_2$) and, also the reference baseline being constant. A constant temperature during the procedure is therefore important, temperature changes could bend the telescope tube for example, hence the procedural requirement to record the temperature and give the system time to equilibrate thermally before starting measurements.

For the plunge and rotate operation in a surveying measurement, where the target is stationary, a single target point is possible. In the case of the star location readings at the latitude observatories, star pairs were used because repeat measurements on the same star are not possible. The averaging was accomplished by using the measurement data from all the observatories, and probably correcting for effects such as temperature. The star pairs used for the measurements were not all exactly on the zenith, so the actual angular measurements were compared to the specified location. Also, a refraction correction was necessary for each reading (following a formula provided by the astronomers).

It is probably better to refer to the telescope reversal technique as “minimizing errors” rather than “free from instrument error.” A precision engineering uncertainty budget for the measurements made by the ILS instruments, including the reversal procedure, would be interesting; however, it would be difficult to produce without access to the actual instrument.

INSTRUMENT PRECISION

Figure 7 presents details of the telescope instrument used by the ILS observatories. This table is photocopied from a NOAA publication from the 1970s. [13] One can argue that the “precision” was not originally specified for the instrument but was based on many years of operation, indicating that the instrument was a highly successful design. The report does not define clearly the units for the scale readings—it reports “in.” to describe angular resolution (resolution is possibly a better word than precision in this case). It appears to be common in the polar motion literature to give angular readings in earth-surface distance, then “10 in.”

would correspond to an angular resolution of only 0.008 arc seconds.

This is an extremely fine scale. The diameter of the vertical circle is given as 24 cm. If a scale was engraved on this diameter circle to a resolution on the order of 0.01 arc second, the line spacing would have to be about 6 nanometer. It would be physically impossible to make a scale with this line spacing. It seems likely that they employed a tangent screw mechanism and then could read the revolutions of a fine screw rather than the scale directly. The uncertainty of a measurement with this scale was possibly orders of magnitude greater than the resolution; however, with a careful observer the readings could have been reproducible, and the averaging of the large data sets could significantly improve the results.

The fabrication of the vertical circle was a key technology in making the VZT system. The making of scales for dividing the circle has been an instrument maker’s challenge for centuries. Quoting from Evans [14]: “Astronomy, navigation, surveying and horology provided the early stimuli for development of accurate circular division systems...” In an 1830 publication, A. Ross is credited with a circular dividing engine: “Claimed resolution is 0.25 arc-sec and accuracy of 2 arc-sec.” By the late 1800s when the ILS instruments were built, accuracies on the order of one arc second were probably possible.

Table 3.--Principal characteristics of visual zenith telescopes used by ILS stations

Item	Value
Focal length	130 cm
Aperture	108 mm
Vertical circle diameter	24 cm
division	10 in.
direct reading (vernier)	10 in.
Level sensitivity	1 in. per division
Micrometer range	40 in.
direct reading to estimation to	0.4 in. 0.04 in.
Construction by	
mechanical	Wanschaf
optical	Zeiss (Jena)
level	Reichel

FIGURE 7. Details of the telescopes built for the ILS observatories. [13]

The primary reference for the VZT was the spirit levels that were used to set the zenith. The description shows a level sensitivity of “1 in. per division”. This would represent a sensitivity on

the order of 0.001 arc second per division. This is possible, but it undoubtedly would present an extreme challenge to the instrument maker. In the early 20th Century, high quality machinist's levels provided sensitivity on the order of 1 arc second, so the levels for the VZTs would certainly have been pushing the available technology. An additional challenge is that spirit levels on most instruments are nulling devices—the bubble is centered in a leveling process. For the ILS the levels were used as indicators to determine the amount of movement from the zenith. So the scales had to be calibrated and the level vials built so that the very long radius of curvature of the glass tube was constant. One of the levels used by on the Gaithersburg VZT is on display at the Gaithersburg City Museum and a close-up photo is shown in Figure 8. The scale divisions are labeled 50 to 90 divisions and they are inverted because the scale was read with a mirror. It is curious why the numbers run from 50 to 90 and not zero to 50.

The setting up of the VZT would have involved adjusting the spirit level. The adjustment screws on the end of the level body is shown in Figure 9. For example, to adjust the level to within 0.5 arc second, the screw adjustments would have to be capable of easily moving the end of the level up or down by less than 0.5 micrometer. This was done with two screws pushing against each other.

This is a design approach that was probably well known to instrument makers well before the 19th Century, but appears to be unknown to some modern instrument designers. The threads need not be very fine or of tight tolerance for this type of adjustment. It depends on the elastic deformation of the screw to make the final adjustment. A simple calculation indicates that the torque required to elastically deform a screw of the size used in the level-adjustment a distance of 0.5 micrometer is on the order of 4 inch-pound (0.5 Nm). This torque would be easy for a person to sense as they were adjusting the level; and, with this torque the screw would be carrying a load of about 20 pounds (90 N), which would keep the level adjustment secure as the telescope was rotated. In Figure 9, the small holes in the heads of the screws show wear which indicates they were often adjusted, and presumably worked well.



FIGURE 8. The unique spirit level for the ILS telescope instrument at the GLO.

Based on the historical procedure description, the observer adjusted the optical micrometer so that the star position was exactly on the reticule line as it passed through the field of view. The range of the optical micrometer was “40 in.” or about one meter of earth-surface (approximately 0.03 arc seconds); so the telescope would have had to be pointed to at least this precision prior to the time for the star to appear in the telescope view. This would set the precision requirement for the scales pointing the telescope—or the minimum angle scale resolution. (It would be interesting to know if a star was ever “missed”, but I could not find any notes regarding that.)



FIGURE 9. Adjustment screws on the spirit level for the GLO instrument.

The resolution of the optical micrometer is listed as 0.4 in. (less than 0.001 arc second). The

optical micrometer on alignment type telescope instruments is commonly an optical flat placed in the path between the objective and the reticule that shifts the line-of-sight to the side a calibrated amount. The limits to the precision are probably the mechanical system that rotates the flat rather than the optical element itself. In effect, it is moving the reticule a prescribed amount. The challenge for the instrument maker is to make the micrometer mechanical system repeatable and the calibration stable over a long time period. This would mean tight mechanical tolerances on the bearings and threads, and overall care in fabrication of the mechanism to allow smooth operation.

From the standpoint of the instrument maker the optical micrometer mechanism of rotating an optical element is probably preferred over making the reticule movable. When making a mechanical device to actually move the reticule there would be a concern of unwanted axial motion shifting the focal point of the objective off of the reticule. Maintaining the focus of the objective accurately on the reticule as the telescope was rotated is an important precision design consideration.

The optical micrometer is used in alignment telescopes used in setting up precision equipment. The design and application of optical micrometers is described in a textbook on "optical tooling". [15]

CONCLUSIONS

The words in the title for this conference: "Precision Engineering and Optics: What are the limits of precision, and how to characterize them?" provided the motivation for this paper. How was the precision specified for the Gaithersburg Latitude Observatory's optical instrument? What were the limits of the instrument? And, is there any value in studying the history of a hundred-plus year old instrument?

The chances are pretty good that there are not any original "specifications" concerning the precision (we would probably use the word *resolution* and *uncertainty* instead of precision) for the ILS instruments. The instrument maker could well have been a member of the scientific team, brought into the project with an explanation of what they were trying to

accomplish, not with a detailed technical specification sent to a fabrication shop.

The guidance of "make the best possible instrument" seems like a reasonable approach for the state-of-the-art instrument in the late 1800s. The instrument maker would have understood how difficult it was (and is) to make measurements in the sub-arc second range. The actual optical elements (lenses) were probably not the limiting factor, but had to be high quality. The limit of the precision of VZT instrument was establishing the line-of-sight of the telescope with respect to direction of gravity. The precision of the mechanical components holding the optics was the key to achieving the goals of the project.

The communication between the scientist and an instrument maker has an interesting history. In early astronomical telescope making, for example in the early 17th Century, Galileo became famous for his scientific contributions. He was also his own instrument maker:

"While other mathematicians besides Galileo were able to figure out the relationship between the focal length of the lenses and the enlarging power of the instrument, he was quickly able to develop remarkable skill at grinding lenses for telescopes—lenses that were outside of the standard repertoire of glassmakers."

It is interesting that Galileo did not document his instrument maker skills (or 'secrets'):

"Galileo gave a synthetic narrative (rich in dates and names but poor in technical details) of how he developed his instrument. He did not tell his readers how he ground lenses—the distinctive skill that gave him an edge over other early telescope makers—nor did he mention the dimensions of his telescopes, the type of glass or the size and focal length of the lenses he used, or the diaphragm he had placed on the objective lens to improve its resolution." [16]

The mechanical systems associated with the most precise telescope fabrication evolved along with the improvement of the optical elements. By the time of the making of the VZTs for the latitude observatories, around the beginning of the 20th Century, arguably the most famous optical scientist, A.A. Michelson, in describing the moving mirror in one of his interferometers, wrote:

“The accuracy of the ways must be so great that the greatest angle through which the mirror C turns in passing along them is less than one second of an arc. This accuracy cannot be attained by the instrument maker, but the final grinding must be done by the investigator himself.” [17]

In the 21st Century we have advanced (possibly some might question the word advanced) to the point where the communication between the instrument maker and the scientist involves specifications along with computer generated drawings, and of course some type of characterization description or inspection routine to satisfy the purchasing department. It is difficult to specify: “give us the best possible instrument.”

Over the last few decades some of us have debated the meaning of the phrase *precision engineering*. My approach: the person taking the responsibility for the final performance of the instrument is doing the *precision engineering* of the instrument, regardless of the person’s title—scientist, technician, precision engineer, mechanical engineer, designer, instrument maker, researcher, machinist, or manager.

To make a state-of-the-art instrument it is still necessary to communicate and work along with the instrument maker, and the researcher may still make some of the final adjustments. The person doing the precision engineering has to understand the capabilities of the instrument maker. By understanding the limits, the precision engineer can specify the required details of, and understand how to characterize, an instrument so that the traditional phrase “make the best possible instrument for our job” is still possible.

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STUCK IN A MOMENT: A VIEW FROM THE MIRE

Patrick Egan, Jack Stone, Jacob Ricker, and Jay Hendricks
National Institute of Standards and Technology
100 Bureau Drive, Gaithersburg, MD 20899

INTRODUCTION

The next-generation pressure standards will be realized via gas density and the equation of state. One way to access the density is through a measurement of gas refractivity, underpinned by the theoretical calculations that predict the relationship between density and refractivity. At present, calculations with sufficient accuracy that link refractivity to density are only available for helium. To measure helium refractivity we employ interferometry to make ultraprecise measurements of the optical length of gas-filled cells and cavities. The refractivity of helium at atmospheric pressure is about 3.2×10^{-5} ; to measure this to 10^{-6} fractional uncertainty—our goal for a pascal realization—would require the measurement of a 15 cm optical length with 4.5 pm accuracy.

We are pursuing two approaches to the measurement of refractivity: a Fabry–Perot (FP) cavity-based system with four interferometers, which we call a variable-length optical cavity (VLOC), and a cell-based heterodyne interferometer, which we call a monolithic interferometer for refractometry (MIRE). Both these optics have been built to precise geometric constraints by silicate-bonding.

The VLOC will measure refractivity as the difference between two 15 cm displacements, one in vacuum and one in helium (where gas pressure and temperature are kept constant during the motion). In actuality we have three interferometers in vacuum surrounding a central interferometer in helium. The main challenge was to minimize the Abbe error: the Abbe offset in this measurement is the deviation of the central interferometer mode from the geometric center of the outer three interferometer modes. In order to minimize the Abbe error, the VLOC has been built with an Abbe offset of less than 70 μm . This feat means that keeping the Abbe error below 1 pm will require measurement and control of angle to better than 20 nrad, which is relatively straightforward given the picometer resolution of FP interferometry. Another feature of the VLOC is that it relies on the flatness of an optical flat to ensure parallel beams in multiple optical cavities and minimize errors as-

sociated with beam walk across imperfect optical surfaces. This requires that local surface slopes have a variation ideally held to less than 1 μrad , where the slopes of interest are on a spatial scale of the cavity mode size (sub-millimeter). Many high-precision optics—including optics with better than $\lambda/100$ figure—may not achieve this specification.

In the case of the MIRE, refractivity will be measured as the change in pathlength through a gas cell, as the cell is filled from vacuum to some pressure. One benefit of a 25 cm multipass gas-cell compared to a 15 cm FP cavity is that it has less stringent requirements on the accuracy and stability of the length metrology, and the fact that the experiment can be performed without motion is a great simplification; on the other hand, a heterodyne interferometer has much lower resolution, and cell window thinning caused by changing pressure can contribute a systematic error of several hundred parts per million. To cancel this systematic error we plan to make refractivity measurements through cells of different lengths but with almost identical material properties, bore-hole and window geometries, and beam incidence. This last requirement—beam incidence—has motivated us build an interferometer that has four beams parallel to two planes within $\pm 85 \mu\text{rad}$.

This proceeding describes the features of both optics,—VLOC and MIRE—the geometric requirements that had to be met to achieve our desired measurement precision, and the steps we have taken to build the optics and satisfy these geometric requirements.

VLOC

A general overview assembly of the VLOC is shown in Fig. 1, and the principle behind the measurement is described in more detail in Ref. [1]. At the heart of the device are four Fabry–Perot (FP) cavities, formed on two optics, a parallel mirror plate and a concave mirror assembly. A photograph of the optics is shown in the inset of Fig. 1: three outer FP cavities in vacuum surround an inner FP cavity which is to be sealed in a bellows and filled with helium (the bellows is not shown

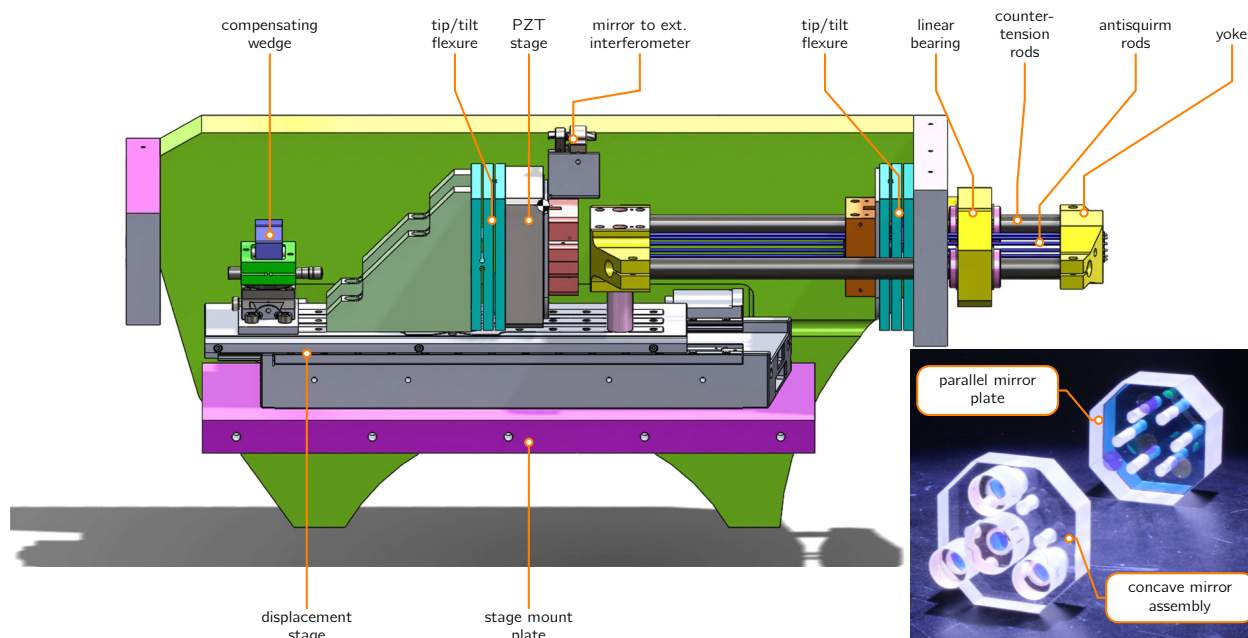


FIGURE 1. The VLOC apparatus consists of a fixed parallel mirror plate inside the tip/tilt flexure at right, and a moving concave mirror assembly inside the PZT stage at left. The displacement stage gives 15 cm of travel, while the PZT stage makes fine adjustments to maintain angular alignment below 20 nrad at the opposite ends of the travel. The inset photograph shows the interferometer optics: concave mirror assembly and parallel mirror plate. The concave mirror assembly has four mirrors silicate-bonded to precise geometric constraint. The span of the octagons are 38 mm.

in Fig. 1). The octagonal shape of the VLOC optics owes its origin to alternative design scenarios that envisioned the bellows sealed to the glass with an o-ring and clamped at four corners; the design we are currently pursuing has the bellows transitioned to glass, and the glass monolithically bonded to the octagon. The six 3.2 mm diameter holes around the center of the octagons are for (tensioned) bellows antisquirm rods: in the mechanicals of Fig. 1, these holes are redundant on the concave mirror assembly.

The concave mirror assembly is housed in the PZT stage which sits on a linear translation stage. In the typical mode of operation the inner cavity will be filled to about 10^5 Pa of helium, and its length is to be changed from 15 cm to 30 cm by the translation stage. (The pressure of helium inside the bellows will be generated by a piston gage: the pressure will be unknown, but constant to $10^{-6} \cdot p$ independent of the volume change.) The outer cavities in vacuum will measure the displacement ΔL , and also monitor pitch/yaw errors in the motion: the PZT stage will be adjusted to correct these motion errors to below 10 nrad. The inner cavity in helium will measure a change in

optical length of $n\Delta L$, and thus the difference between the displacements in the outer and inner cavities is a measure of refractivity $n - 1$. This measure of helium refractivity at a known temperature can be used to realize the pascal, as discussed for example in Refs. [2, 3]. In other words, we will use the measured helium refractivity to determine the unknown pressure generated by the piston gage.

These refractivity measurements are expected to happen in the near future. In addition to pitch/yaw at sub-10 nrad, we will also be monitoring straightness/flatness at sub-1 μm : the metrology supporting this apparatus is a large undertaking and will not be described here. The purpose of this section is to describe the characteristics of the interferometer optics at the heart of the VLOC.

Parallel mirror plate

The parallel mirror plate consists of four mirrored portions, (6.5 ± 0.5) mm in diameter, coated on a 20 mm thick, flat substrate. The HR coating was specified as 99.7% at 633 nm and 1542 nm. (The mirrored portions were masked so that the bellows to glass transition can be bonded and sealed

to bare glass on the parallel mirror plate.) Since the mirrors are deposited directly on the plate, the plate had to be wedged at 9 mrad to avoid parasitic reflections entering the cavities (an extra precaution in addition to the AR coating on the back-surface). Because we intend to translate the concave mirror assembly by $\Delta L = 15$ cm, motion errors during translation will inevitably cause beam walk across the parallel mirror plate, and if the mirrored portions are out-of-parallel by ϕ , this beam walk effectively introduces an alignment error between the interferometers approximately $n\Delta L(\frac{\phi^2}{2} + \theta\phi)$ [1]. Here, θ is the misalignment of the central mode direction with the direction of the displacement of the moving mirrors, and $\theta + \phi$ is the misalignment of one of the outer interferometers. Typically, θ is much larger than ϕ . It will vary with position due to the stage straightness (about 2 μm) and due to roll (30 μrad), so that it is typically larger than 7 μrad regardless of how well overall alignment is achieved. The modes of the three outer interferometers are on a “bolt circle” of 13 mm radius, and the mode diameter is about 0.3 mm: the specification requested of the manufacturer was that all four mirrored regions were to be parallel to 1 μrad , and that surface spatial variations were to be less than 1 nm/mm. It is notable that a $\lambda/100$ surface flatness would not necessarily satisfy this unique parallelism requirement. The manufacturer achieved the 1 μrad parallelism before coating, but stresses induced by the masked coatings changed surface form. The parallel mirror plate was subsequently measured at NIST with a Fizeau interferometer. In Fig. 2 we show surface flatness for each of the outer mirrors relative to the central mirror. The slope of the coatings are within 2 μrad of parallel (ie, $\phi = 2$ μrad) to one another, which means that misalignment errors between central and outer interferometers should contribute less than 2 pm error to the displacement measurement. (Interestingly and not apparent in Fig. 2, is that the slope of the coatings relative to the substrate is 15 μrad , and the height of the mirror stacks are about 6.5 μm .)

Concave mirror assembly

The concave mirror assembly is a complicated optic. Its main requirement was to maintain a very stable relationship between the inner and outer mirrors, and also to keep the Abbe offset below 100 μm . For the symmetric case, this is equivalent to saying that the centers of curvature of the three outer mirrors must be located on a circle whose center is concentric within 100 μm of

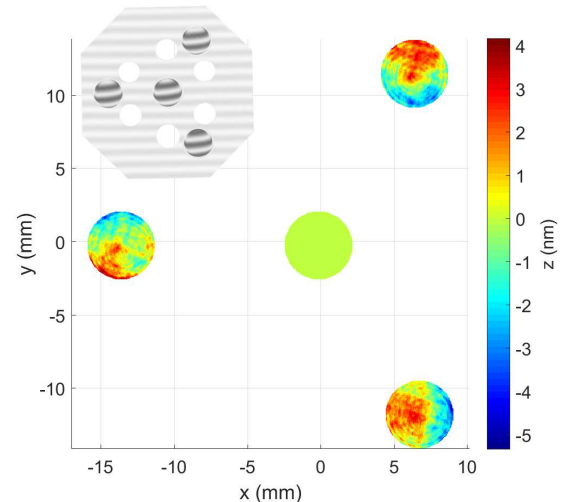


FIGURE 2. Profile of the four HR-coated regions of the parallel mirror plate: The slopes of the three outer coated areas relative to the central coated area are within 2 μrad . Inset: fringes on the Fizeau flatness interferometer.

the center of curvature of the central mirror. To our knowledge it is not presently feasible to polish multiple concavities into one substrate and meet this 100 μm concentricity requirement, so positioning and bonding four separate mirrors to one baseplate seemed the viable solution. Additionally, the central interferometer (and the bond between the optics and the bellows-to-glass stub) needs to be leak-tight, and any bonding technique must be ultrastable. These requirements led us to conclude that silicate-bonding [4] was the best (if not only) solution.

The optic consists of a 10 mm thick baseplate (parallel window) with four 6 mm holes drilled through it to allow beam transmission. The four concave mirrors were face-bonded to the parallel window using silicate-bonding, following the LIGO recipe [5]: we used a 4 : 1 ratio of distilled water to sodium-silicate solution, approximately 0.8 $\mu\text{L}/\text{cm}^2$ solution per bond area, and all surfaces were flat to $\lambda/10$. Our approach of face-bonding four separate concave mirrors to a flat baseplate means that a flat annulus must remain on the substrate into which the concavity is polished. It is also desirable that the annulus of the central mirror be as large as possible to minimize leak rates. These requirements must be put in context to a size constraint of 38 mm-square which was placed on our baseplate by the aperture of the PZT stage: all this meant that our inner mirror substrate had a diameter of 13.5 mm and

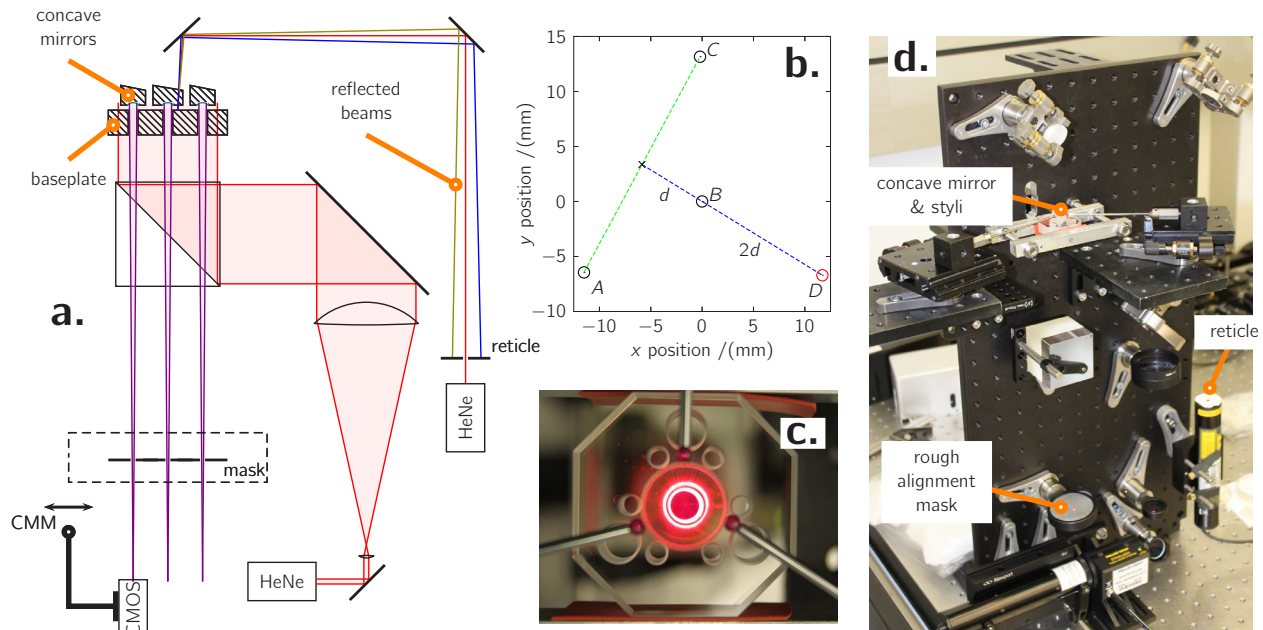


FIGURE 3. (a) Schematic of the jig used for positioning and bonding the concave mirror assembly. (b) The position of the third outer mirror D was found as a projection of the distance $2d$ through the center B from the midpoint of the line AC: the points A, B, C, D refer to the locations of the mirror focal points. (c) Photograph of center mirror located by ruby styli while bonding. For scale, the diameter of the center mirror is 13.5 mm. (d) The actual jig being used for bonding with the rough alignment mask. To bond the final mirror, the mask was removed, and a camera attached to the ram of a CMM probed the mirror focal points.

the outer mirrors had diameters of 10.5 mm; we specified a diameter on the extent of the concave polish of (7 ± 1) mm. The sagitta of a 0.5 m arc extending 7 mm is only $3 \mu\text{m}$, and not only was our tolerance on polish depth/diameter challenging, the best that could be done with centering the polish relative to the mirror substrates was ± 1 mm. The lack of concentricity in the concave mirrors meant that we could not use the mirror substrate as a dimensional reference: we had to position and bond the mirrors while looking at their actual focal points. Also note: The lack of concentricity between the concave polish and the coating mask meant that the focal point was the only point at which we could reliably gage the relative locations of the four interferometer modes. (The coating stack was also undersized relative to the concave polish, so as to permit face bonding between the baseplate and outer annulus of the mirror substrate.)

The principle behind assembling the optic was to nudge the mirrors around so that the locations of their focal points met the near zero-Abbe offset requirement. For nudging the mirrors we used three ruby styli mounted on manual translation

stages, shown in Fig. 3(c). In practice and for the most reliable bonding, the assembly proceeded by first bonding the inner mirror and two of the outer mirrors in a cleanroom with rough alignment, and then bonding the last outer mirror in a lab with a coordinate-measuring machine (CMM) that told us precisely where the focal point had to be. Our assembly jig, shown in Figs. 3(a) and (d), worked as follows: We used a $\times 300$ beam expander and HeNe laser to produce a collimated beam of about 100 mm diameter, and this extra large beam was directed to the baseplate. (As mentioned previously, the separation between the outer mirrors is 26 mm, so 50.8 mm optics offered a large enough aperture.) When a concave mirror was placed on the baseplate, its coating reflected a very small part of the large collimated beam and focused it to a point at its focal length. For coarse positioning of the first three mirrors in the cleanroom, we used an aluminum plate as a rough alignment mask, which had 0.2 mm diameter holes drilled in locations where the centers of curvature were nominally supposed to be; we bonded the inner and two outer mirrors so that three beams reflected and focused from these mirrors passed through the holes in the mask.

This rough alignment and bonding was judged by eye, and is shown in process in Fig. 3(d).

For ease and convenience, we did not aim to achieve an equilateral triangle with the focal points of our first three coarsely positioned mirrors, and therefore lacking symmetry, the zero-Abbe position of the third outer mirror does not follow the concentric circle geometry previously mentioned: instead, as in Fig. 3(b), the third outer mirror was positioned a precise distance along a line from the mid-point of the line between the two outer mirrors projected through the point of the central mirror. When positioning the final mirror the coarse alignment mask was removed, and we probed the focal point of beams reflected from the mirrors with a camera mounted to a CMM. [For reference, an analogous setup with a camera on a CMM is shown in Fig. 4(c).] The positional uncertainty of the CMM was $2\text{ }\mu\text{m}$. We used the centroid of the camera image as the reference, and moved the camera (arm of CMM) so that the centroid of each beam was referenced to the same pixel location: we had reproducibility of $6\text{ }\mu\text{m}$ locating the focal points of the three coarsely positioned mirrors. As mentioned previously, there is a lack of concentricity between the concave polish and coated region, which meant that the relative separations of the reflected beams changed as a function of distance (typically less than $1.7\text{ }\mu\text{m}/\text{mm}$) and we had to translate the camera at the focal plane: we were confident of locating the focal plane to 3 mm . Once the focal points of the three coarsely placed mirrors were found, the position of the third outer mirror was calculated, the camera moved to that calculated point, and the mirror was nudged to position and bonded so that its focal point was at the reference position on the camera. We achieved an Abbe offset of $62\text{ }\mu\text{m}$.

A minor complication in the concave mirror assembly is that the concave mirrors are polished into wedged substrates to avoid parasitic cavities. Since the concave mirror assembly is the moving part of the VLOC, beam alignment and coupling into the cavities would be translation dependent. To mitigate this problem, we employ the compensating plate shown in Fig. 1, which has a wedge nearly identical to that of the concave mirror substrates but rotated in azimuth so that the two prisms effectively cancel. However, effective cancellation of the beam deviation demanded that we also ensure the rotation of each mirror—

wedge orientation—was nominally the same. To achieve this, we used a second laser as a beam pointer, also shown in Fig. 3(a) and (d). For the bonding of each mirror, we directed the beam pointer so that it passed through the outer edge of the mirror substrate. This resulted in two reflections: one from the wedged back-surface of the mirror and one from the baseplate. (In actuality, the reflection from the baseplate overlapped the reflection from the front-surface of the mirror substrate—and this became a single reflection after bonding.) We positioned a reticle in front of the pointing laser: the reticle was simply a line on a piece of paper placed on the laser head (ie, the reflection was deliberately directed almost back into the laser tube). Our premise was that if each mirror was rotated such that the two reflections end up on the line of the reticle, then the wedge of each mirror was orientated the same. Our premise relied on the fact that the baseplate is flat, and that the reticle did not rotate between the bonding of each mirror. However, the approach was independent of the angle and relative location of the incident beam.

MIRE

MIRE is based on the classical Tanaka-style interferometer [6], in which we have a gas cell in the measurement arm and a vacuum cell in the reference arm. We actually implement this in a multipass scheme with a triple-cell, where either the central cell or the two outer cells can serve as a reference arm while the other serves as the measurement arm, as in Fig. 4(a). The body of the triple-cell is a single piece of borosilicate crown glass with three holes through it, and at each end a single piece of glass is silicate-bonded forming windows for the three holes: knowledge of cell length is a prerequisite for accurate refractivity measurements, and the thin and repeatable ($100 \pm 50\text{ nm}$) bond interface allowed us to accurately measure cell length on a CMM before the windows were bonded. Unlike the VLOC, which is designed in such a manner that it can be operated at constant pressure, the critical measurement for the MIRE is a change in optical pathlength when gas is admitted to the measurement arm. Effects of pressure distortions [7] must be taken into account by building at least two such triple-cells, one short and one long. This is a simpler alternative than a possible approach based on a variable length cell, analogous to the VLOC but with a moving window replacing the moving mirror, as discussed next.

In most refractometers such as the MIRE, the reference and measurement beams pass through the same thickness of glass windows. This arrangement will largely compensate various effects including thermal variations of the windows and changing pathlengths that will occur if the window tilts. The compensation also will eliminate the effect of any uniform wedge in window thickness, which might otherwise cause an unacceptably large variation in pathlength accompanying even microscopic changes in where the beam passes through the window. However, the compensation will be imperfect due to non-flatness of window surfaces, and the situation for a variable length cell with a moving window then becomes entirely analogous to the problem of beam-walk across a non-flat mirror coating in the VLOC, where variations in surface slopes of the optical element between the positions of the measurement and reference beams will give rise to errors. As a practical matter the problem is somewhat more difficult to remedy for a cell-based approach than for the VLOC. For this reason and for reasons of mechanical simplicity, we completely abandon the moving window scheme with its requirement of having equal glass paths independent of cell length. Our approach is based on two (or more) fixed-length cells, where the window thicknesses will ideally be matched at the micrometer level but need not be matched at the picometer level! The critical measurement is not a change in pathlength for a known displacement but changes in pathlength when gas is admitted to the short and long cell. Under these circumstances the requirement that the window optical pathlength is independent of cell length is replaced by a requirement that, for any given fixed length, the variation of the window pathlength with cell pressure is the same for the short and long cells. This can be assured if the window does not move transversely to the beam when pressure is changed (beam-walk), and if window thinning due to the pressure change is the same for both cells. The second requirement is more demanding than the first but can be met if the dimensions and geometry of the windows are near-identical, if the mechanical and optical properties of the windows are near-identical (glass made from the same melt), and if the beams pass through all windows at approximately the same position relative to the underlying holes. This last requirement means that beams must be parallel to each other so that they pass through the same window position in cells of different length. We now describe

how the interferometer was built, and the steps we have taken to ensure similar beam incidence between long and short triple-cells.

For the MIRE apparatus stability is a chief concern, and our requirement is rather challenging: a thermodynamic measurement like refractivity has a timescale of several hours (0.1 mHz), and we need the interferometer to be stable below 100 pm during the measurement. By comparison, if extrapolated to 0.1 mHz, the stability requirement of the LISA Pathfinder interferometer is 5 nm [8]. We follow the LISA approach for interferometer stability, and have all optics silicate-bonded [4] to a 42 cm $\times$ 15 cm $\times$ 2.5 cm fused silica baseplate. A photograph and schematic of the baseplate part of the interferometer are shown in Fig. 4(a) and (b). All bonding surfaces were optically polished to $\lambda/10$ flatness: the baseplate has $\lambda/10$ local flatness over 6.5 cm², and 3λ global flatness. Most optics were bonded directly to the baseplate (fiber launch, polarizing-beamsplitters, mirrors, and Jamin-beamsplitter); whereas others were face-bonded to one another (polarizer and waveplates to polarizing-beamsplitters, collimating lenses to the fiber launch, and wedge prism-pairs to a mount block). The face-bonding was done with uncoated surfaces, and we typically observe reflections less than 0.03 % from the bond interface.

Fiber-launching (beam-pointing) began by inserting flat-polished and antireflection (AR) coated fiber ferrules into 20 μ m clearance holes which set the 22 mm separation between the beams. The ferrules were epoxied into this tight-fit, and the depth of insertion was adjusted to collimate the beams at 2.1 mm diameter. In process we found a shear plate and viewer the most practical method of judging collimation, and we subsequently used a wavefront sensor to measure beam divergences of 0.190 mrad and 0.254 mrad, which are close to the 0.192 mrad of perfect collimation. Two aspheric lenses (plano-convex) were face-bonded along their 1 mm outer annulus to the fiber launch block, and were nudged into place with three ruby styli on micrometer stages to set the beams to be parallel. (The collimating lenses are AR-coated, and this outer annulus was the one bond that involved a coated surface.) We configured a CMM to translate a camera parallel to the polished baseplate surface, and beam-pointing was adjudged by looking at the image-centroid at near and far positions. See

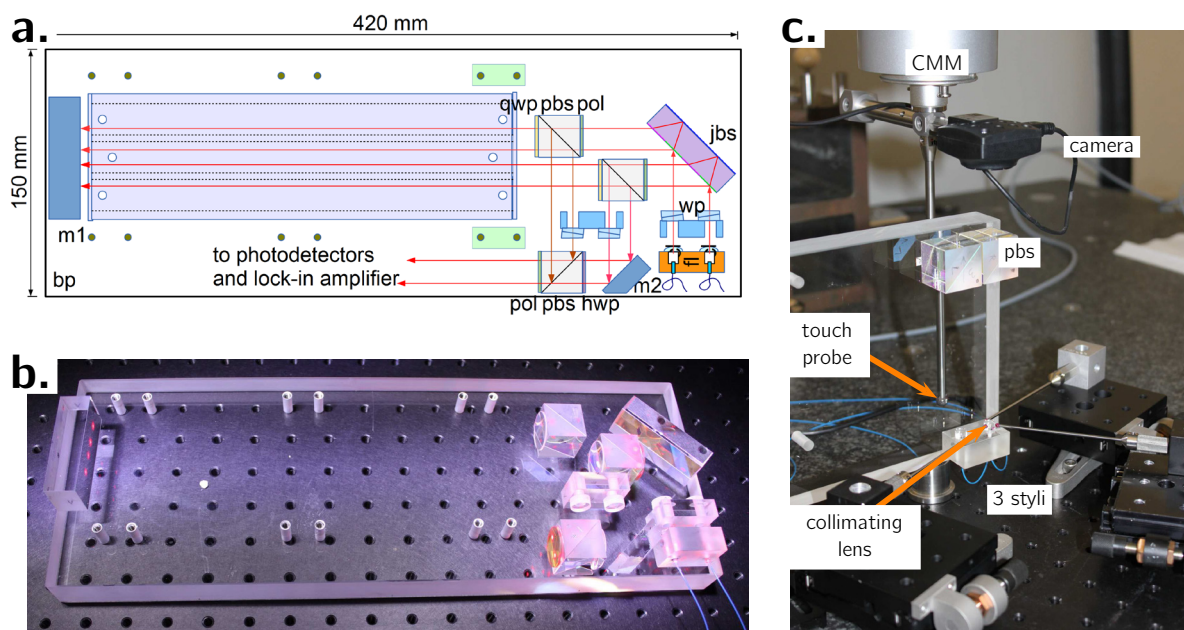


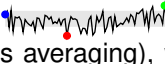
FIGURE 4. (a.) Schematic of the interferometer, with components drawn to scale. (b.) Photograph of MIRE, without gas triple-cell. Threaded invar inserts are epoxied into the baseplate to clamp the inlet/outlet o-ring flanges to the triple-cell. (c.) Setup to align beam-pointing. The collimating lens is positioned by three styli on translation stages. The camera on the CMM is vertically translated by about 20 cm: the camera measures the beam centroid at near and far positions. A touchprobe is used to build a coordinate system so that the camera runs parallel to a desired plane (eg, the plane of the polished baseplate).

Fig. 4(c) for how this setup looked in practice. We achieved beams out-of-parallel to one another by $(220 \pm 15) \mu\text{rad}$. This residual error in beam alignment necessitated the 9 mrad wedge prism-pairs to further correct beam-pointing, a point we will return to momentarily.

After the fiber launch the two beams are separated into four by a Jamin-beamsplitter, which consists of alternating 0.25 % and 50 % reflective-coated portions on its front surface and a 99.5 % reflective-coating on its back surface; the parallelism on the Jamin-beamsplitter was $10 \mu\text{rad}$, and its thickness sets the 12 mm reflected beam separation. The four beams reflected from the Jamin-beamsplitter are polarized with a 10^5 extinction ratio, and a quarter-waveplate set the polarization state such that more than 99.3 % of the beam returning from the fold-mirror was reflected by the polarizing-beamsplitter. The polarizing-beamsplitters are epoxy-free, and the transmitted beam deviation through the assemblies was as large as $250 \mu\text{rad}$; which is why the aforementioned lens bonding was done with the beamsplitters temporarily in place, as shown in Fig. 4(c). The bonding surface of the fold-mirror and Jamin-

beamsplitter were polished within $100 \mu\text{rad}$ of perpendicular to the coated surfaces. Two 0.4 mm diameter aperture jigs were located relative to the straightedge of the baseplate and temporarily tacked in place at the extremities between the Jamin-beamsplitter and eventual location of the fold-mirror: the Jamin-beamsplitter was nudged into position and bonded so as to maximize the intensity passing through both apertures on a large-area photodetector. At this point of the build, we returned to the CMM and locked-in the final beam-pointing with the wedge prism-pairs. The CMM was configured to run parallel to the polished surface and straightedge of the baseplate: referencing beam alignment to both these planes is advantageous for reproducible positioning of triple-cells of different length. We achieved beam alignment parallel to these two planes within $\pm 85 \mu\text{rad}$. The resolution of our beam-pointing setup was about $50 \mu\text{rad}$, as determined by the $6 \mu\text{m}$ reproducibility of the image centroid, the few micrometers form of the planes to which the CMM was configured to run parallel, and the 15 cm travel of the CMM axis. In hindsight, we believe an improvement factor of two to three could be gained with a more clever ar-

rangement on the CMM (ie, more travel) and with wedge prism-pairs closer to 1 mrad (our choice of 9 mrad was based on what is available off-the-shelf).

The aforementioned aperture jigs were once again tacked in place and referenced to the straightedge, and the fold mirror was nudged into position and bonded so as to maximize the intensity returning through the two apertures (as observed reflected by the polarizing-beamsplitters); this setup ensured that alignment of the returning beams were referenced to the baseplate straight-edge within 100 μ rad; subsequent positioning of the triple-cells (of different lengths) can initially be achieved by caliper measurements referenced to this straightedge, and reproducibility ensured by hard-stops (ie, spheres epoxied in place). The result of these efforts is that when we measure gas refractivity in triple-cells of different lengths, we are confident beam incidence is the same for all window pairs to within 0.2 mm. After the beams are returned through the triple-cell and reflected from the beamsplitters, they are combined in a final halfwaveplate-beamsplitter-polarizer optic and sent to photodetectors, outside the vacuum chamber in which the MIRE sits. Before interference, we employed another wedge prism-pair to optimize visibility: these prism-pairs are a minor weakness of the design, because the glass pathlength imbalance introduces a temperature dependent drift of about 26 pm/mK; during refractivity measurements we typically have sub-mK temperature stability. Differential phase is measured with a lock-in amplifier. As of writing, MIRE is demonstrating sub-50 pm stability over 15 h  (min: -44 pm, max: 45 pm, 600-s averaging), which in a 12.5 cm long triple-cell would correspond to a fractional error of less than $2 \times 10^{-6} \cdot p$ for a pascal realization at 200 kPa. We are in the process of taking refractivity data in triple-cells of different lengths, and these measurements will be the subject of a future article.

OUTLOOK

We have described the precision engineering behind optics at the heart of two ultraprecise refractometers built at NIST, the VLOC and MIRE. The refractometers will be used to realize the pascal via measurements of gas density and the equation of state. We described some of the unique features of the VLOC optic, among which is a 2 μ rad parallelism between mirrored regions 26 mm apart, a 2 nm/mm local flatness, and an

Abbe offset of 62 μ m. The critical feature of MIRE is that, while keeping the build quasi-monolithic and ultrastable, two pairs of beams launched from two fibers have been aligned to two planes within $\pm 85 \mu$ rad of parallel.

At the present time, MIRE is running and taking data, and the experiment will soon be described in more detail. The VLOC apparatus is nearing completion, and initial characterization tests are expected to begin late 2017.

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LIMITS TO RESOLUTION IN OPTICAL DIMENSIONAL METROLOGY

Peter de Groot, Leslie Deck and Vivek Badami

Zygo Corporation, Middlefield, CT USA

DEFINING RESOLUTION

Resolution is one of the most frequently misunderstood and poorly defined descriptions of performance [1]. Misconceptions include the role of spatial and temporal bandwidth, the effects of filtering, and fundamental effects related to physical optics, detector noise, and aberrations.

A contributor to the confusion is lack of precision in the definition of the term “resolution”. In optical imaging, resolution refers to the ability to clearly distinguish between closely spaced points or features, often according to limits established by physical optics. In distance and surface height measuring systems, resolution is limited by digitization or measurement noise. To better understand the limits of resolution and how to characterize them, we need to consider these two concepts independently, and perhaps adjust our vocabulary accordingly.

RESOLUTION IN 2D AND 3D IMAGING

With densely-sampled images, we encounter limitations based on diffraction or optical quality that limit the ability of instrumentation to resolve closely-spaced image features [2]. Lateral resolution is the smallest center-to-center separation of features that still allows us to see clearly that there are two features present [3]. FIGURE 1 shows on the right two trenches formed by patterning silicon on a quartz substrate. The interference microscopy 3D image shows that there are indeed two lines present, although they appear blurred at this high magnification. As the center-to-center separation between the lines decreases, the optical resolving power of the instrument becomes a limiting factor in determining whether the two lines are clearly separated.

Modern laser Fizeau interferometers have cameras with over 5 million pixels, providing detailed lateral sampling of form, waviness and surface texture [4]. The topography image in FIGURE 2 is for a diamond-turned disk with a surface height range of a few tens of nanometers. To take advantage of these large-format cameras, the optical system must be appropriate quality, with large limiting apertures.

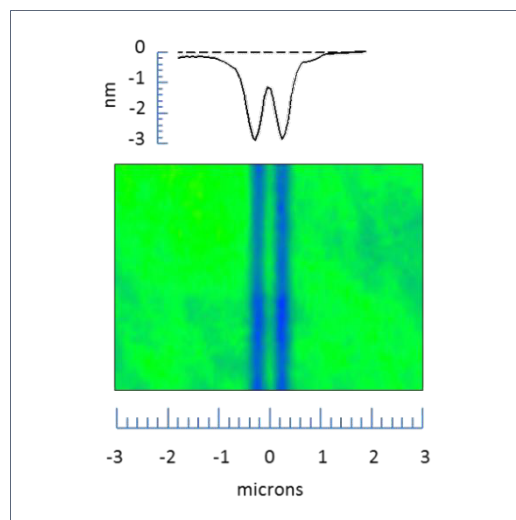


FIGURE 1: 3D interference microscope image of parallel trenches using a 100× objective with an NA of 0.85. The trenches are 200 nm wide and the center-to-center spacing is 440 nm

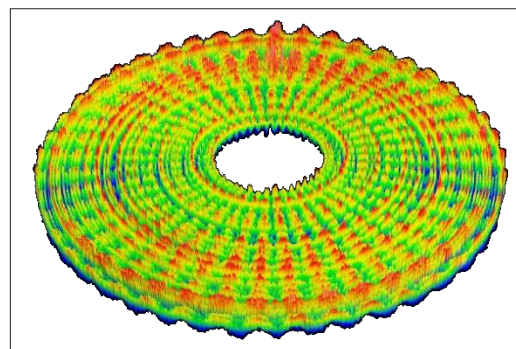


FIGURE 2: 3D image of a diamond-turned disk using a 100 mm aperture laser Fizeau interferometer.

It is common to quantify the lateral or imaging resolution of an instrument in terms of a single number, such as the Rayleigh limit [5]. Although more difficult to specify, the modulation transfer function (MTF) and its analog the instrument transfer function (ITF) in 3D metrology provide much greater information regarding instrument response as a function of line separation [6].

A simple linear ITF catalogs the response of the system to pure surface sine wave patterns as a function of frequency. In the limit of small

amplitude ($\ll \lambda$) or low frequency sine waves, we can predict the instrument response to the overall surface structure by mapping the Fourier components of the surface, weighted by the ITF, to the reported topography [5]. An attractive feature of the ITF characterization is that it corresponds closely to the power spectral density (PSD) evaluation of surface error in optical fabrication [7]. The foundations for a rigorous understanding of the MTF and the linear ITF are well documented in the literature [8]. More complete models allow for extending these ideas more generally to larger steps and slopes in coherence scanning 3D microscopy [9, 10].

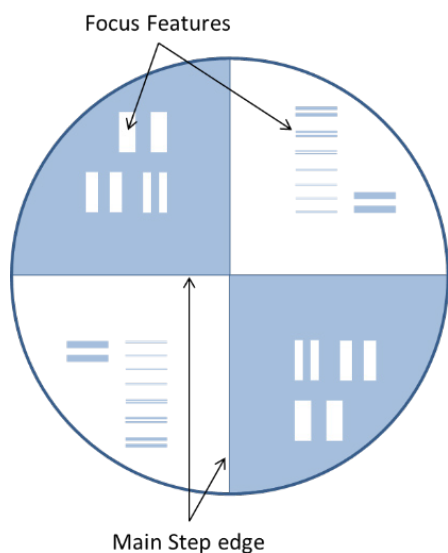


FIGURE 3: Patterned surface of an ITF measurement specimen with etched features for evaluating the edge spread function.

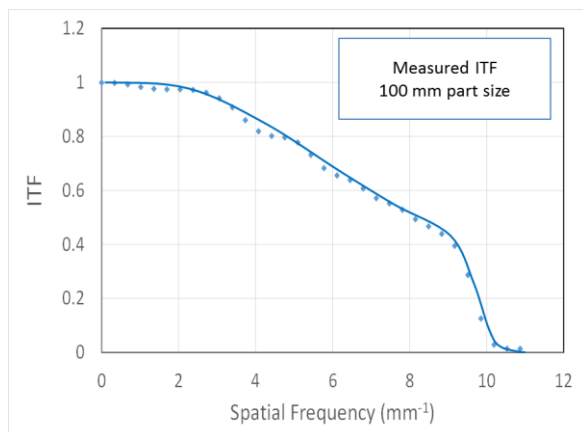


FIGURE 4: Example precision ITF measurement results showing a design resolution limit of 0.0625 mm or 1600 cycles/aperture.

For our measurements of ITF, we use the 3D equivalent of the edge spread function [11], relying on 25 nm sharp step features across the 100 mm full aperture of a super-polished disk. The test sample shown in FIGURE 3 has both a horizontal and a vertical edge, and additional targets to aid in properly focusing the instrument. The ITF calculation follows from comparing the frequency content of the measured step to an idealization of a perfect step.

FIGURE 4 shows an ITF measurement result, in this case for an instrument with a design maximum spatial frequency of 16 cycles/mm. Measurements at different field positions verify that the expected spatial frequency response is uniform over the full surface area. The steep slope at high frequencies is consistent with apertures in the optical system intended to prevent camera aliasing [12].

An important aspect of imaging resolution is that it relates to fundamental physical principles that have little to do with measurement noise. We can average the image in FIGURE 1 for days if we like, and perhaps gain an extra 10% in resolution simply from the improved quality of the image. But ultimately we are limited by the imaging principle, the wavelength, and the apertures within the optical system. Similar constraints apply to depth discrimination in confocal microscopy and optical coherence tomography. These limits do not apply, at least not in the same way, to our next topic.

RESOLUTION IN DISTANCE MEASUREMENTS

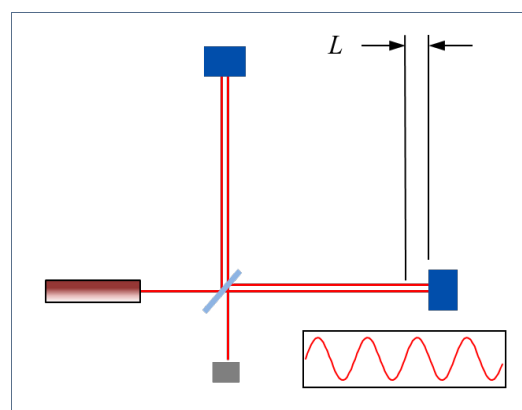


FIGURE 5: Interferometer for measuring the distance L .

Optical dimensional metrology typically involves one or more distance, displacement or surface height measurements with respect to a virtual

reference point or plane in space. FIGURE 5 shows an interferometric sensor for measuring a distance or displacement L . The smallest detectable change δL is often called the “resolution”. Of significance, δL is not related to the separation of two object points or distances at the same time, as in imaging resolution. As such, there is no absolute physical limit regarding how small δL can be. There is no Airy spot with which to contend as a fundamental limit.

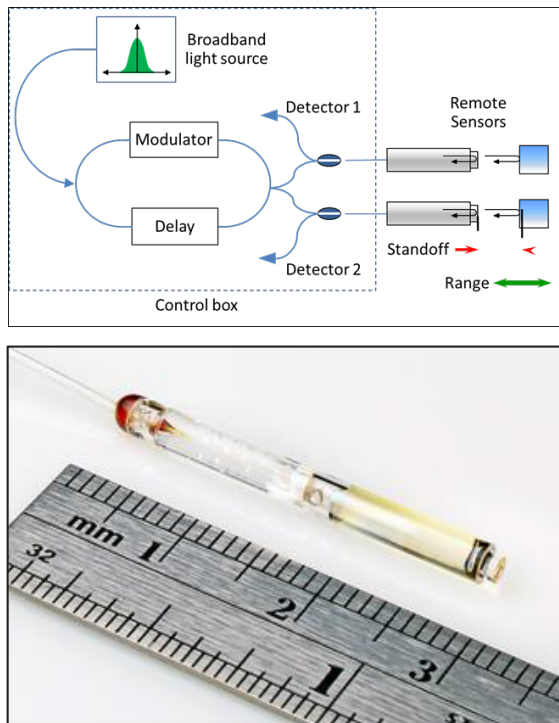


FIGURE 6: High-precision, fiber-based position sensing system [13, 14].

Assuming that we have a surplus of digitization for recording fine increments, the “resolution” of distance sensors is synonymous with random measurement noise. Consequently, proper specification necessarily includes the bandwidth or measurement time [1]. It also follows that with enough light source power, time and signal to noise, there is no lower limit to what is achievable in detector sensitivity. Modern precision sensors such as the fiber-based device shown in FIGURE 6 have 3σ noise figures in the 0.5 nm range at 10 kHz, compared to >250 nm limit for the lateral resolution in visual wavelength microscopy.

TABLE 1: Specifications for the precision sensing system shown in FIGURE 6.

Fiber sensor specifications [15]			
Digital Resolution	0.01 nm	Noise (3σ)	0.005 nm/ $\sqrt{\text{Hz}}$
Data Rate	Max 208 kHz	Stability	1 nm / day
Channels	Up to 64	Non-linearity	± 1 nm

RESOLUTION IN SURFACE TOPOGRAPHY

Areal surface topography presents an interesting and sometimes confounding mixture of the two resolution concepts discussed here. The measurement in 3D measuring microscopy provided by interferometers, confocal and focus variation instruments is an array of height measurements over an imaged surface area. The ability to separate features on the topography map is related to imaging resolution, whereas the change in surface height that we can detect is governed by measurement noise. It is unfortunate that there is widespread use of the term “vertical resolution” for such instruments, as it promotes confusion of the two concepts. This is evident in specification sheets for these instruments, which, with few exceptions, make no mention of measurement speed or time bandwidth when quoting vertical resolution [16]. Draft ISO standards for calibration will perhaps mandate a correction to this practice [17].

Apart from model-based methods comparable to scatterometry and enhancements to image clarity and correction for undersampling, 3D interference microscopes remain diffraction limited. Surface height “resolution”, on the other hand, has room to advance based on quantum well depth, camera pixel count and data acquisition speed. The current state of the art for interference microscopy, for example, provides better than $0.1\text{nm}/\sqrt{\text{Hz}}$ over one million simultaneous height measurements [18]. Following the familiar $\sqrt{N}$ rule means that a 10 pm repeatability is achievable in 100 s. It is reasonable to consider methods and enabling technologies that can reduce this even further without breaking any fundamental limits.

THE LIMITS OF RESOLUTION

Here I have argued that characterizing the limits of resolution requires an adjustment in our vocabulary. It would be preferable in specification sheets and technical reports to reserve “resolution” for those metrology attributes that are constrained by our ability to clearly separate neighboring features or surface depths, as in 2D and 3D imaging. For distance measurements, including height measurements for widely-separated surface features, it would be better to quantify the measurement noise or the equivalent, taking care to note if it is a single standard deviation or a multiple thereof.

Once we are speaking a common language, there is a wide range of useful characterization techniques. For imaging resolution, sample specimens with closely-spaced features complement linear ITF methods based on the edge spread function and other techniques. For distance measurements, the corresponding noise levels can usually be determined from repeatability tests over a range of bandwidths. These characterization methods allow for sensible comparison of instrumentation, advances in performance, and adaption of measurement techniques to demanding applications.

ACKNOWLEDGEMENTS

The authors recognize the contributions of ZYGO scientists and engineers to the results shown in this paper. We also thank Prof. Richard Leach for his review of the draft manuscript.

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Fizeau interferometers: the best nulling autocollimators money can buy?

Chris Evans¹, Theo Ruijl², Mike Holmes³, Leslie Deck³ and Jim Soobitsky³

**¹University of North Carolina at Charlotte
Charlotte, NC, USA**

**²MI Partners
Eindhoven, NL**

**³Zygo Corp
Middlefield, CT, USA**

We have used Fizeau interferometry to independently measure stage angular error motions with uncertainties of less than 10 nrad in a complex opto-mechanical system. The system measures high performance aspheric lithography lens elements interferometrically by mapping a sequence of annular zones while a precision 6 degree of freedom stage translates the lens element along the optical axis of the asphere up to 500 mm.

In-situ calibration of the stage motion errors, especially of straightness of travel, required ultraprecision angle metrology. Dynamic considerations drove the use of a redundant

displacement measuring interferometer (DMI) design with a compact metrology frame for 6 degrees of freedom. The unit length was realized in-situ to significantly better than 1 ppb using an I₂ stabilized HeNe laser. The DMI sources themselves have 1 ppb (k=3) wavelength stability over any hour and fluctuation in the 500 Pa nominal air pressure is corrected in real time using an in-situ wavelength tracker. Combined with mK level thermal stability, we were able to achieve nm level motion errors over the system's full range. Doing so taught us a great deal about the real and imagined limits of using Fizeau as autocollimators, with some lessons learned.

CHARACTERIZATION OF ADVANCED LIGO CORE OPTICS

GariLynn Billingsley, Hiroaki Yamamoto, and Liyuan Zhang

LIGO Laboratory

California Institute of Technology

Pasadena, California, USA.

INTRODUCTION

Precision optical measurement is a core technology contributing to the success of the Advanced LIGO detectors [1]. The round trip cavity loss requirement for each 4 kilometer LIGO arm is < 75 parts per million. The loss budget based on mirror requirements is estimated at 64 ppm. The fabrication of these low loss cavity mirrors relies on accurate characterization of the optics. The metrology at the heart of this process delivered coated optics with an average surface figure error of 0.4 nm rms over a diameter of 160 mm and an estimated Fabry-Perot cavity loss of 45 ppm. The power spectral density curves for different spatial frequency ranges are presented; these provide insight into sources of measurement error.

THE LIGO INTERFEROMETERS

The Advanced LIGO gravitational wave detectors are second generation instruments designed and built for the two LIGO observatories in Hanford, WA and Livingston, LA. The two instruments are identical in design, and are specialized versions of a Michelson interferometer with 4 km long arms as shown in Figure 1. As in initial LIGO, Fabry-Perot cavities are used in the arms to increase the interaction time with a gravitational wave, and power recycling is used to increase the effective laser power. Signal recycling has been added in Advanced LIGO to improve the frequency response. The primary operating parameters are summarized in Table 1.

REQUIREMENTS

The LIGO optics must have few defects and very small surface aberration in order to store high power, large beams in the 4 kilometer long interferometer cavities. In addition, the substrates and coatings must have low absorption to minimize thermo-elastic distortions. Defects and small spatial scale surface aberration, of order 1 cm or less, contribute to power loss in the cavity. Larger scale surface aberrations are tolerated as long

as they are symmetric and do not destroy the near-perfect contrast required as the beams recombine at the dark port of the interferometer. Excess absorption in the coatings and substrates can lead to thermal lensing and surface deformation, which can also destroy the contrast at the dark port.

While the term Core Optics applies to all of the large optics in LIGO resonant cavities, we limit this discussion to the test masses that form the Fabry-Perot cavities, since these have the most stringent requirements. The design basis for these optics is that total losses are low enough to meet the 75 part per million round trip cavity loss budget for the LIGO Fabry-Perot cavities while minimizing the effects of Brownian thermal noise [2][3].

Using the largest spot size possible minimizes Brownian thermal noise. We set this size at 6.2 cm radius on the ETM, setting the diffraction loss at 1 ppm. This large beam size exacerbates the problems of excess loss due to phase distortion from figure error.

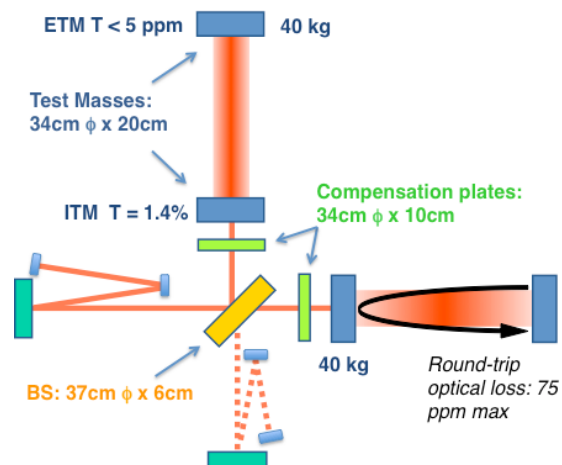


FIGURE 1. The LIGO detectors are specialized Michelson interferometers. Each 4 km arm contains a Fabry-Perot cavity formed by the Input Test Mass (ITM) and End Test Mass (ETM).

Requirements for the Advanced LIGO optics are calculated using a custom built code called “Stationary Interferometer Simulation” (SIS). [4] This is a numerical simulation package for an interferometric optical system, which is comprised of optical resonators and telescopes. The field in the interferometer is calculated using a fast Fourier transform. Realistic details of the optical components can be included in the simulation. These details include: size, measured or modeled mirror surface and lens transmission maps. Optical resonators are set close to the actual operating conditions, and the power loss caused by mirror surface aberrations and field distortions are calculated. The simulation results are used to quantitatively set the optics requirements.

TABLE 1. Main parameters of the Advanced LIGO interferometers.

Parameter	Value
Arm cavity length	3994.5 m
Arm cavity finesse	450
Laser type and wavelength	Nd:YAG, $\lambda = 1064$ nm
Input power, at PRM	up to 125 W
Beam polarization	linear, horizontal
Test mass material	Fused silica
Test mass size & mass	34cm ϕ x 20cm, 40 kg
Beam radius ($1/e^2$), ITM / ETM	5.3 cm / 6.2 cm
Radius of curvature, ITM / ETM	1934 m / 2245 m

Cavity Loss Budget – the test mass surface

The round trip cavity loss is required to be less than 75 ppm. The LIGO design uses known, repeatable, state of the art optical performance characteristics in combination with the SIS model to allocate losses and estimate the performance of the optics as designed.

Defects

The primary contribution to cavity loss is scatter due to point defects, scratches, streaks and contamination. The requirements on these defects are partitioned among the various sources according to experience and are generally budgeted as geometric occlusion. The total budget for loss due to defects and contamination is 13 ppm per mirror.

Figure Error

The second largest contribution to cavity loss comes from the figure error of the test masses. This is budgeted at 24 ppm for the combined error in the Input Test Mass (ITM) and End Test Mass (ETM) mirrors. The corresponding requirement is that each test mass surface must have a root mean square figure error of less than 0.3 nm over the central diameter of 160 mm, after subtraction of power. The model for this budget assumes that the surface error is similar to the iLIGO optics in amplitude and spatial distribution.

Absorption

A corollary to the figure error requirement is the loss due to absorption in the test mass coatings. Though the loss is budgeted at 0.5 ppm per mirror the 750 kW of circulating power creates a heat load of up to 0.4 Watts in each test mass. The thermo elastic deformation of the mirror surfaces is symmetric in the cavity, an error which scales differently than the random figure error described above. However, this 0.4 Watts is absorbed into the bulk of the ITM, causing a significant thermal lens in the resonant cavities of the near Michelson arms of the interferometer. The LIGO design includes a thermal compensation subsystem to manage this dynamic lensing [5].

Microroughness

There is prompt loss in the arm cavities due to high spatial frequency roughness of the test masses. This is scatter that promptly exits the cavity, not hitting the opposite mirror even once. The spatial scale for this is greater than 1/mm. The requirement in this region is that the amplitude be less than 0.16 nm rms in the center 120 mm diameter of the optic. Using the classic phase grating analysis [6] gives us a budget of 7 ppm for the total cavity loss.

End Mirror Transmission

Coating transmission for the ETM is specified at less than 5 ppm. This is based on the best-known performance of Ion Beam Sputtered (IBS) coatings.

Substrates

Fused silica is the substrate used for all of the LIGO Core Optics. The material has low mechanical loss [7], which is critical in test mass optics. Fused silica is also available in grades with very low bulk absorption and scatter [8].

FABRICATION

The Advanced LIGO test masses are made from Suprasil 3001 and 312 for the ITM and ETM respectively. The test masses were polished by Coastline Optics in Camarillo, CA, and ASML Optics in Richmond, CA, under subcontract to L3 Communications Integrated Optical Systems-Tinsley. ASML is now known as Zygo Extreme Precision Optics. The polishing process, proposed by ASML was a two part process involving a super-polish followed by Ion Beam Figuring (IBF.) The test masses were coated using IBS by Laboratoire Des Materiaux Avances (LMA) in Lyon, France.

Substrates

Suprasil 3001 is used for the ITMs where low absorption and good homogeneity are critical. Suprasil 312 is used for the ETM where the transmitted beam is used for control and diagnostics. There are total of 20 Suprasil test masses, 12 at time are needed for instrument operation, the remaining 8 are in process spares.

Polishing

A summary of some of the key polishing characteristics are compared to the results reported by ASML, as shown in Table 2. Due to a need for symmetry, the LIGO radius of curvature requirements are broken into two components; 'absolute accuracy' and 'precision.' The absolute accuracy describes the error budget for assuring the absolute radius of curvature. This budget is fairly large, -5, +15 meters. This is 1% of the final radii shown in Table 1. The precision requirement pertains to the grouping of as-built optics, they must measure the same (within their class) to within ± 3 meters [9].

TABLE 2. As-built attributes of the polished aLIGO test masses reported by ASML, now Zygo Extreme Precision Optics, are well within the aLIGO requirements.

Test Mass Attribute	Spec.	Average of 20
Radius of Curvature precision	± 3 m	± 1.1 m
RMS (160 mm ϕ) $\lambda_s > 1/\text{mm}$	< 0.3 nm	0.13 nm

ASML uses IBF in order to sculpt the back side of the ITM to compensate for inhomogeneity in the bulk material. This provides for a near perfect wave front as the beam enters the arm cavity through the bulk of the ITM. This same process is used on the compensation plates, which are suspended next to the ITMs. The combined optical path length of the compensation plate and ITM is 435 mm, so this correction of the bulk homogeneity is critical.

Coating

Test masses are coated by Ion beam sputtering. The masses are coated two at a time in order to achieve the best possible match in transmission for the two arm cavities. This symmetry is critical in order to minimize contrast defect at the dark port of the interferometer. A double planetary system is used by LMA in order to achieve required uniformity for two masses coated at once [10]. During the development of the planetary system LIGO provided large area surface figure metrology. The phase map of the uncoated surface is subtracted from the phase map of the coated surface, providing a high-resolution map of coating uniformity. An example of the earliest ETM uniformity attempts in the planetary coater is shown in Figure 2.

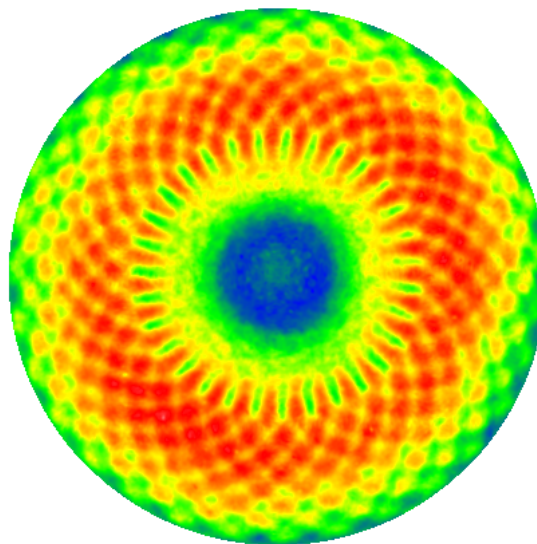


FIGURE 2. Phase map showing characterization of coating uniformity in the early stages of the large planetary coater at LMA. Peak to Valley is 4.2 nm and rms is 0.7 nm. This plot shows only the center 160 mm of the mirror.

The coating uniformity was subsequently improved to suppress the spiral pattern. The final optics show no sign of this pattern, though a small spherical aberration remains. The power spectral density (PSD) of the uncoated and final coated surfaces are compared in Figure 3. These data are from different instruments.

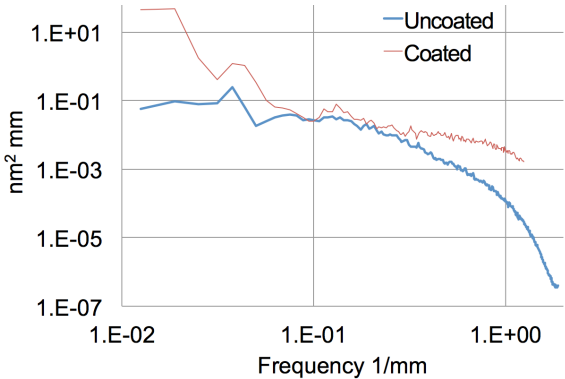


FIGURE 3. The final coating uniformity achieved is demonstrated in a 160 mm aperture PSD of the uncoated and coated ETM15. The uncoated data are provided by ASML. The coated data are from the Caltech measurement.

CHARACTERIZATION

The fabrication of low loss cavity mirrors relies on accurate measurement of the optics. The LIGO lab played the central role in monitoring the quality of optics under development, and confirming the quality of final deliverables. LIGO lab used a custom commercial Fizeau interferometer to measure surface figure throughout the polishing and coating process. A custom scanner was used to characterize absorption, scattering, reflection and transmission over the active aperture of the optics [11].

The goal of the characterization effort was not just to inspect for compliance with the specification requirements. Instead, the goal is to provide the best knowledge of the optical characteristics that could be provided within budgetary constraints. The final use of the characterization data is in the SIS code. This code can use realistic maps and thus predict and help diagnose interferometer performance.

Scatter

The total integrated scatter (TIS) from an HR coating is measured using an integrating sphere.

The 1064 nm output of a JDSU NPRO laser is regulated by a half-wave plate and a Faraday isolator. Photo diodes are placed before and after the optical chopper to monitor the power of laser beam going into the integrating sphere. The polar angle collection range of the integrating sphere is defined by the sizes of the top and bottom ports. The current set-up has a collection range from 1.0° to 75°, corresponding to a bandwidth of spatial frequency 17 – 910 mm⁻¹. The collected scatter is measured with a photo diode coupled to the output port of the integrating sphere. Calibration is accomplished by installing a diffuse reflectance standard (Labsphere Spectralon) at the bottom port and normalizing the measured signal to the incident power.

Scatter is measured over two diameters; 50mm and 160 mm. These measurements are performed with a step size of 0.3 mm, and 1 mm respectively. Both scans use a 0.3 mm probe beam diameter. The results of both are quite similar, an example of the small step size scan is shown in Figure 4. The figure contains a histogram of the measured values. The average scatter of all test masses is 9.5 ppm.

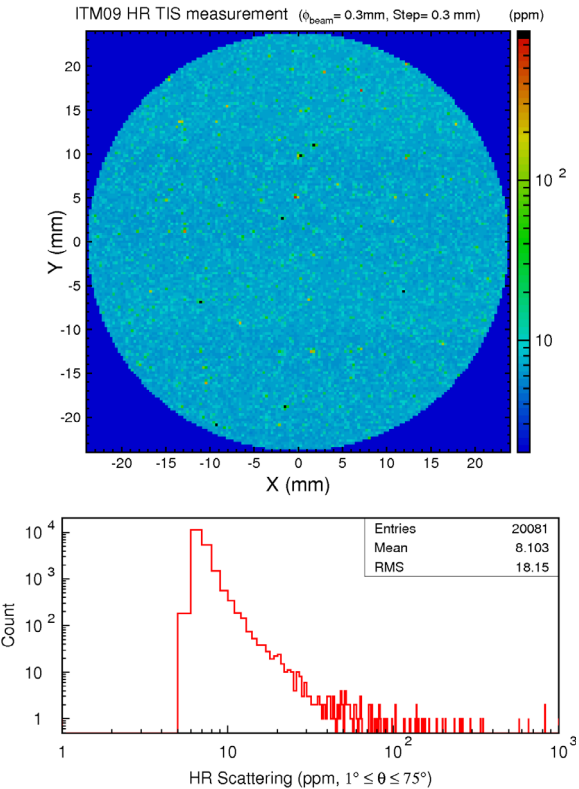


FIGURE 4. Scatter report for the center 50 mm of a LIGO ITM shows a mean scatter of 8 ppm.

Absorption

Absorption at the ppm level in the bulk substrate and coatings is measured using Common-Path Interferometry [12]. Qualitatively, a chopped high power Nd:YAG beam is focused on the sample and the optical path difference (OPD) is probed by a low power CW He-Ne beam with a beam size of a few times larger than that of the chopped Nd:YAG beam. The OPD changes due to the absorption caused by thermal refractive index change (dn/dt) and thermal expansion. At a distance of (~ 10 mm) from the interaction point, the perturbed center of the He-Ne beam interferes with the non-perturbed part of the beam and converts the absorption induced OPD into an intensity signal. This signal is read through a pinhole with a photodiode and a lock-in amplifier.

The absorption scans are performed on the coating outside 60 mm radius in order to avoid any risk of damage from the high power pump beam to the center of the optic. The pump laser operates at 0.5 MW/cm^2 during measurement of the lowest coating absorption. Four linear scans of 20 mm in length are carried out along $\pm X$ and $\pm Y$ outside central 60 mm radius. The calibration is done using a ring-down cavity HR mirror, where the absorption was measured during a ring-down test. The average absorption for all test mass coatings is 0.29 ppm.

Reflection And Transmission

Reflection and Transmission are measured on the same apparatus as scatter and absorption. The measured signals are normalized to the incident beam. In this case the measurement beam is 1 mm and the step size is 1 mm. A transmission scan of ETM10 is shown in Figure 5. The mean transmission in the center 160 mm diameter is 3.9 ppm, with an rms of 1.4 ppm. There are several spots of high transmission, none exceeding 200 ppm. The average transmission for all ETMs is 3.9 ppm.

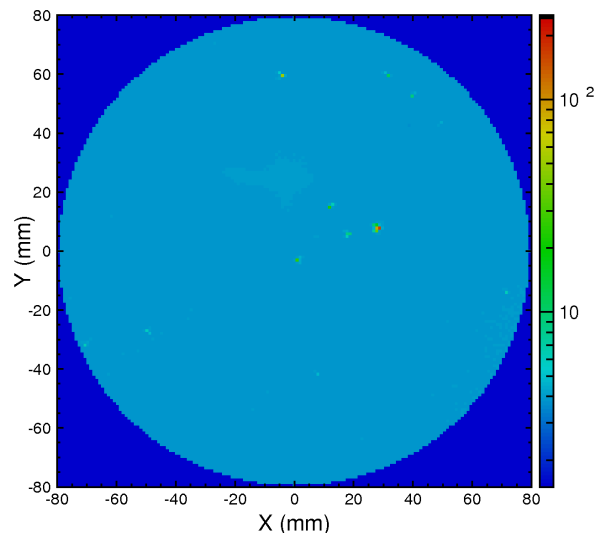


FIGURE 5. Transmission map of ETM10 showing a few spots of < 10 and one exceeding 100 ppm transmission. The color scale is in ppm.

Figure Measurement

Of particular importance in the measurement at the LIGO Lab is understanding surface figure error and PSDs derived from it. The estimation of the systematic error of the data is difficult and several techniques were used to get proper estimation of the error. The transfer function delivered with the Fizeau interferometer showed inefficiency around the 1 mm spatial scale region without magnification, this was also observed in the data of the polished surface. This inefficiency is analyzed by comparing data sets measured with and without magnification.

The Zygo interferometer commissioned by LIGO has four fixed magnifications. There are two collimators; in addition, a turret magnifier can be put into position in front of the camera to magnify each collimator image by a factor of 2. The lowest noise data come from the unmagnified collimators. Figure 6 compares PSD curves taken in the center 30 mm diameter of ETM11. Of particular interest is the difference in the 10X data, obtained by the same method, but using different cavity lengths. Close visual inspection of the datasets suggests that defects in the transmission sphere are in better focus in the shorter cavity (80mm) and are slightly defocused in the longer cavity.

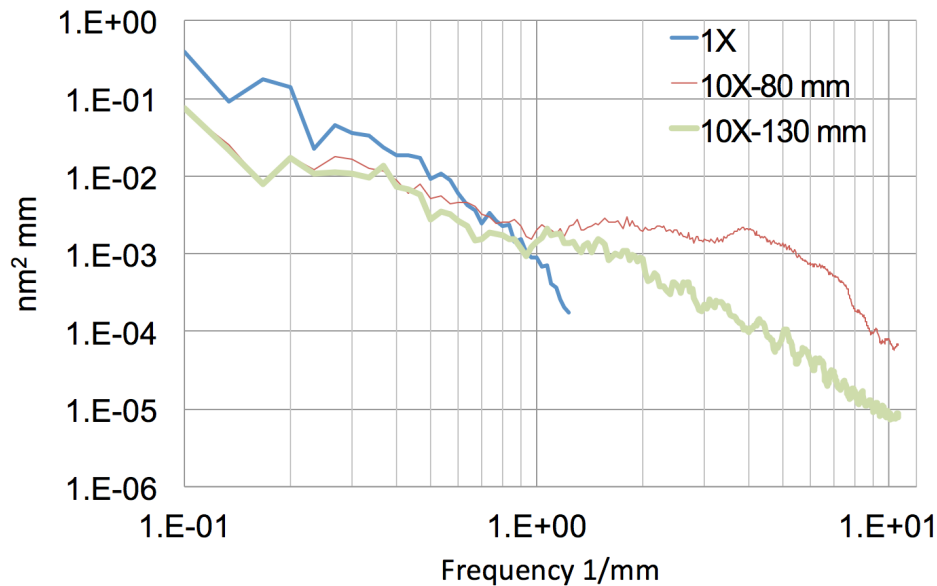


FIGURE 6. Data from the center 30 mm diameter of an ETM optic are compared using two different fixed magnifications and two different cavity lengths. Considerable noise is added when the reference sphere is too close to the test piece.

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HIGH-PRECISION LATERAL DISTORTION CORRECTION IN COHERENCE SCANNING INTERFEROMETRY USING AN ARBITRARY SURFACE

Rong Su¹, Peter Ekberg² and Richard Leach¹

¹Manufacturing Metrology Team
The University of Nottingham
Nottingham, UK

²Department of Production Engineering
KTH Royal Institute of Technology
Stockholm, Sweden

INTRODUCTION

Lateral optical distortion is present in most optical imaging systems. In coherence scanning interferometry (CSI) [1,2], distortion may cause field-dependent systematic errors in the measurement of surface topography. Distortion, unlike optical aberrations such as spherical aberration, coma and astigmatism, is not responsible for a lack of sharpness of the image [3]; rather is related to the form of the image, and the degree of the distortion is dependent on the position in the image plane. Optical aberrations are usually suppressed for optimisation of the optical resolution in a commercial CSI system, but a significant amount of distortion can be present [4,5]. In areas such as computer vision, distortion in a camera system may cause errors in pattern analysis and recognition, dimensional and displacement measurement, 3D image reconstruction, etc. [6]. In a CSI system that measures the 3D topography of a surface, distortion may cause errors in the dimensional measurement in both lateral and height directions. Thus, distortion should be corrected in order to achieve a non-distorted image field and a uniform lateral sampling distance of the image projected on to the camera, such that the measurement accuracy can be improved.

Two steps are usually required to correct lateral distortion: 1) correct the form of the image, e.g. when imaging a perfect grid pattern the systematic deformation of this grid should be removed so that the measured grid has uniformly distributed nodes; 2) absolute calibration and adjustment of the scale, i.e. the scale of the image should be made traceable to the definition of metre (a linear scaling problem).

The traditional calibration and adjustment of the lateral distortion for a 3D imaging system

combines the two steps listed above into one by measuring a standard artefact. The artefact usually contains a grid of precision manufactured patterns, mostly with rectangular or circular shapes, e.g. an areal cross grating standard [7]. The distortion is then calculated as the deviation of the measured positions of the patterns compared to their nominal positions. The nominal positions are calibrated by a traceable metrological instrument, e.g. a stylus instrument [8]. The manufacturing and calibration processes of the standard artefact are often complex and expensive.

Self-calibration techniques may also be used for distortion correction [4,5]. When using self-calibration techniques, the distortion correction may be separated to the two steps mentioned above. However, current self-calibration methods still require an artefact with a grid of manufactured patterns. After the separation of the distortion and the shape error of the artefact, the absolute scale may be determined using a linear scale artefact that is traceable to metre.

The major disadvantages of the traditional correction and current self-calibration methods are their strong dependence on structured grid patterns. Problems include: 1) the accuracy of the locations of the patterns limits the accuracy of the distortion correction if traditional methods are used; 2) the tolerance on the manufacturing quality of the surface topography (form and texture) is very high; 3) the edges of the pattern structures need to be sharp and clean, and the uncertainty of the necessary edge detection algorithm degrades the accuracy of the distortion correction; 4) care must be taken when handling and storing the artefacts in order to ensure low levels of damage or contamination; and 5) availability – for correction of different imaging

systems with different lenses and magnifications, a range of grids with suitable pitches is required.

In this paper, a new method for the correction of lateral distortion in a CSI system, based on a simple sub-pixel imaging correlation method and self-calibration, will be demonstrated. Instead of using a manufactured grid pattern, an arbitrary surface is used and a precision of a few nanometres is achieved for the distortion correction. Here, an arbitrary surface refers to a surface that contains some deviations from flat and has some features (possibly just contamination), such that feature detection is possible. This surface could be a rough surface, e.g. a regular machined metal surface or the surface of a coin, or it could be a smooth surface with random features or structured patterns, e.g. a scratched and defected mirror or an areal cross grating. These surfaces can be easily found in a laboratory, manufacturing workshop and even an office. The method demonstrated in this paper may significantly enhance the precision of the distortion correction of 3D optical imaging systems. The cost of the artefact may be reduced significantly and even to zero if the absolute scale is not considered.

METHODS

Extraction of 2D intensity map in CSI

In CSI, the height variation is encoded into the phase of the complex wave amplitude [1,2]. The low spatial frequency term of the CSI signal corresponds to the intensity signal without interference effects and can be extracted by using low-pass filtering (see *FIGURE 1*). The extracted two-dimensional (2D) intensity map will be used for further analysis. The CSI instrument used in this study has a precision piezo-electric drive with a maximum scan speed of 96 $\mu\text{m/s}$. The specifications of the objective lens used in this study are given in Table 1.

TABLE 1. Specifications of the objective lens

Magnification	Type	NA
50×	Mirau	0.55
Field of view (FOV) / μm	Optical resolution / μm	Lateral sampling distance / μm
166.912	0.52	0.163

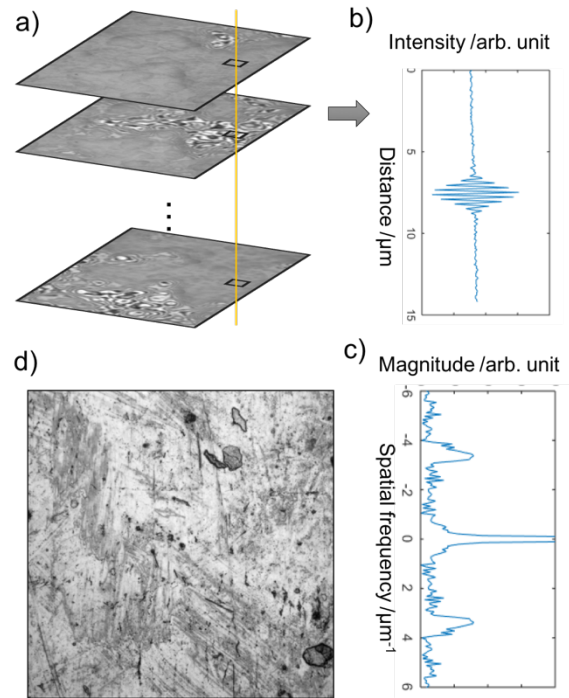


FIGURE 1. Extraction of 2D intensity map in CSI. a) Interferometric image stack in CSI; b) interference signal recorded by a pixel; c) the Fourier transform of the signal, where the low frequency part corresponds to the intensity map; d) the extracted intensity map.

Image correlation method

Image correlation methods have been widely applied in many areas, such as the measurement of stress, strain and displacement in the field of mechanics [9]. In this study, the first step of the image correlation method is the definition of the grid of image patches in the 2D intensity map extracted from the measured CSI data of a surface. Usually measurements from three different views are required for a self-calibration process [10]. The first view is called the reference view, based on which the grid is defined (see *FIGURE 2*). The translated and rotated views are defined as the measurements of the same area of the surface but being slightly shifted and approximately 90° rotated, respectively. The defined grid on the arbitrary surface is equivalent to the physical grid manufactured in a standard artefact. One of the advantages is that the location and pitch of the defined grid is known and may be easily adjusted so that any spatial frequency of the distortion can be captured.

Searching for the exact positions of the image patches (e.g. see *FIGURE 2(b)*) in the translated and rotated views is based on the calculation of

the cross correlation function between two image patches; one from the reference view and the other from the translated or rotated view. This process does not require high precision and is carried out by using a priori information of the translation and rotation, i.e. the translated distance and the degree of the rotation angle. The cross correlation function can be expressed as

$$C_{I,J}(p, q) = \sum_j \sum_i f(i - I, j - J) g(i - I + p, j - J + q),$$

where (I, J) is the centroid position of an image patch in the reference view in the image coordinate system (i, j) , which corresponds to the x and y directions in the spatial domain, respectively. Functions $f(i, j)$ and $g(i, j)$ are the intensity maps of the image patches in the reference view and the translated or rotated view, respectively. Because the images of all three views are aligned using a priori knowledge of the translation and rotation, the searching area can be as small as a few pixels around (I, J) . The pixel coordinates (p, q) , corresponding to the correlation maximum, are the desired quantities. The entire set of (p, q) for all image patches provides the location of the defined grid with pixel-precision in the translated or rotated view.

The result of the correlation function is refined with sub-pixel precision, which can be achieved by resampling the image patches using a cubic interpolation combined with a simple iterative method for searching for the correlation maxima. In principle, the grids calculated from the repeated measurements of the reference view should overlay the defined grid, and the grids calculated from the other views should appear distorted.

2D self calibration method

Reversal techniques and other forms of self-calibration have been used for centuries in order to calibrate machine tools and metrology instruments [10]. The mathematical theory behind the 2D self-calibration technique in metrology systems was developed by Raugh [11]. A numerical approach for solving the self-calibration problem, based around the concept of iteration, was developed by Ekberg et. al. [12]. In general, 2D self-calibration is based on the assumption that the artefact will not change its shape regardless of how it is mounted in the measuring instrument, i.e. it is a rigid body. Then the apparent shape of the artefact will change when it is observed at different positions in the

measuring instrument, if lateral distortion is present. It is possible to separate the distortion and the real shape of the artefact, by using at least three measurements of the same artefact with different placements. The typical placement scheme contains a reference measurement, a translated measurement and a measurement with the artefact rotated. The details of the 2D self-calibration method can be found elsewhere [12].

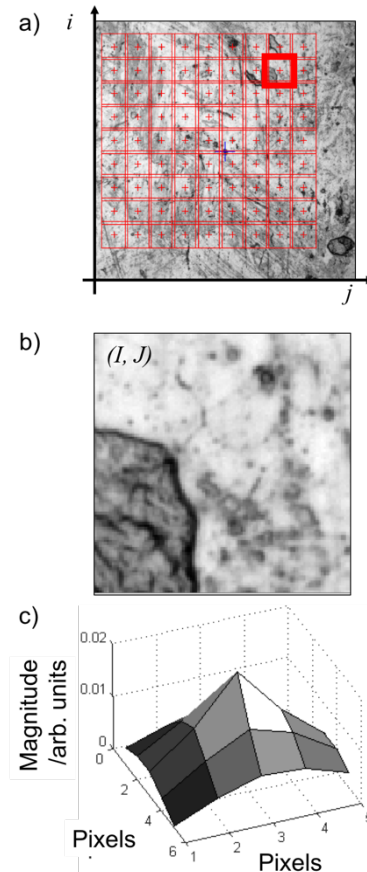


FIGURE 2. Illustration of the image correlation method. a) A 9-by-9 grid of image patches defined in the 2D intensity map of the CSI measurement of a coin surface; b) the enlarged image patch (100-by-100 pixels) of which the centroid is located at (I, J) in the image coordinate; c) the correlation function which is the result of searching for the reference image patch around (I, J) in the translated or rotated views.

RESULTS

The first step of the distortion correction is the generation of the 2D intensity map. As shown in **FIGURE 3**, the 2D intensity maps of the three views (reference, translated and rotated) are extracted from the measured 3D CSI data of a

coin surface, and the image patches are defined based on the intensity map of a single reference measurement. The height information in *FIGURE 3(c)* is used for the estimation of the best focus position along the scanning axis.

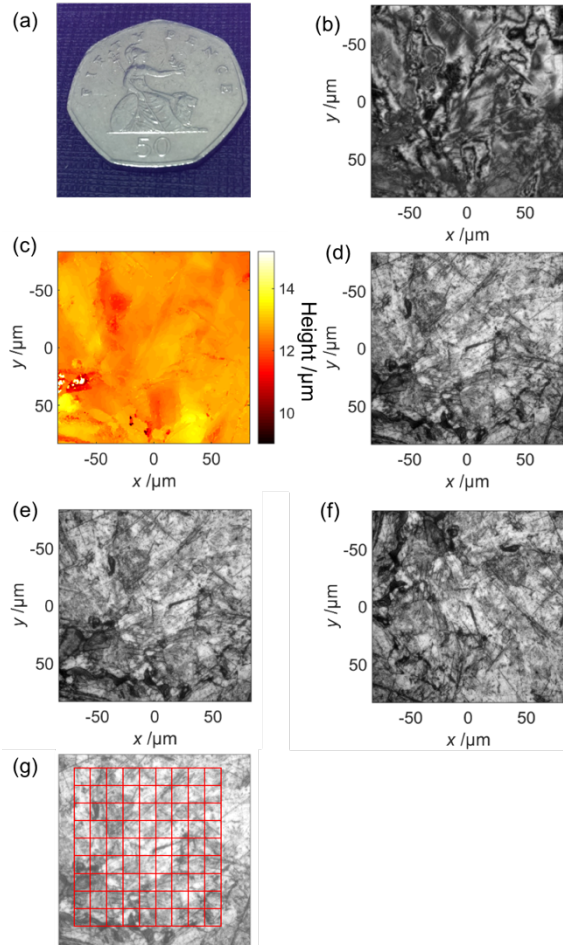


FIGURE 3. Extraction of the 2D intensity maps from CSI measurements of a coin surface and definition of the grid. a) Picture of the coin; b) an image slice from the 3D fringe data; c) calculated height map; d,e,f) Intensity maps of the reference, translated and rotated views, respectively; g) defined grid and image patches. The grey scales of all intensity images are normalised. The 50× objective lens was used.

The nominal grid is defined as a perfect square area containing 9-by-9 image patches as shown in *FIGURE 3(g)*. The centroids of the image patches are calculated using the described image correlation method, such that the grid can be presented by 9-by-9 nodes. *FIGURE 4* shows the grids that are calculated from the repeated measurements of the three different views. The measured grids from the translated and rotated

views are distorted because of the presence of optical distortion. This procedure is repeated five times to evaluate the measurement repeatability and overlay of the instrument. The overlay is the limiting factor of the precision of the self-calibration method, and is calculated as the standard deviation of the distances between the measured positions of the nodes (centroids of the image patches) and the corresponding mean position calculated from the five measurements. The measured grids are shown in *FIGURE 4(a)* to *FIGURE 4(c)*. It should be noted that the repeated measurements are not overlapping perfectly, which indicates possible lateral drifts of the image domains of the repeated measurements. The drift can be evaluated as the relative offsets of the centroids of the grids (see *FIGURE 4(d)* to *FIGURE 4(f)*).

The measured grids can be aligned with their centroids for all the nodes in order to calculate the measurement overlays. The calculated overlay is around 1 nm for all three views. The overlay is mainly associated with the measurement noise that is influenced by environmental changes, vibration, electronic noise, etc. [13].

The drift is of the order of tens of nanometres and may be caused by the motion of the axial scanner or the translation stage. The drift of the entire image domain can hardly be noticed in a scanning-type microscopic imaging system where the lateral sampling distance in an image is usually in the range from 100 nm to 6 μm. The drift may degrade the repeatability of the instrument.

The input to the self-calibration algorithm is the measured grids from the three different views, as shown in *FIGURE 5(a)* (aligned with the centroids for display purposes). The output is the distortion map (*FIGURE 5(b)*), from which the distortion function and the correction function can be obtained. By applying the correction function to the three measurements, the distortions in the translated and rotated views can be corrected (see *FIGURE 5(c)*). Due to the system noise, the distortion cannot be completely compensated. The overall quality of the self-calibration result is evaluated by the residual errors after the distortion correction, and the evaluation is carried out by calculating the root-mean-square (RMS) value of the residual errors for the three views. The result is given in Table 2. The RMS residual errors is approximately 2 nm over a FOV of 160 μm. A result with similar precision has also been

achieved over a FOV of several millimetres for a lens with much lower magnification (to be reported in a later publication).

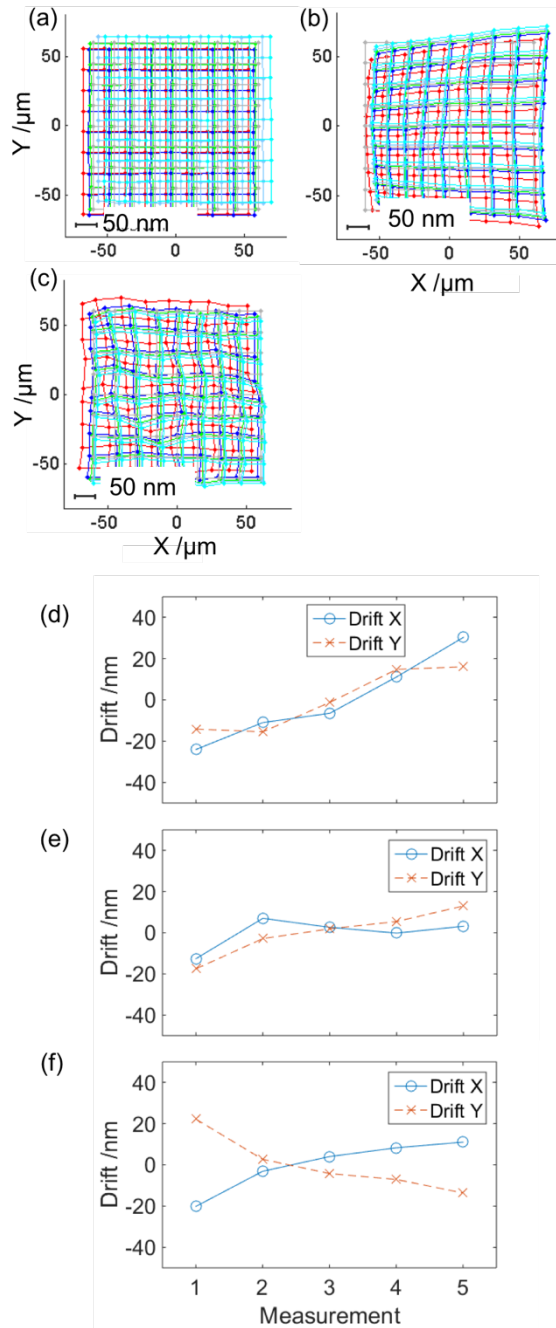


FIGURE 4. Measured grids from three different views. a,b,c) Resulting grids (absolute coordinates) of the repeated measurements of the reference, translated and rotated views, respectively (the errors are magnified for visualisation purpose); d,e,f) calculated drifts for the reference, translated and rotated views, respectively.

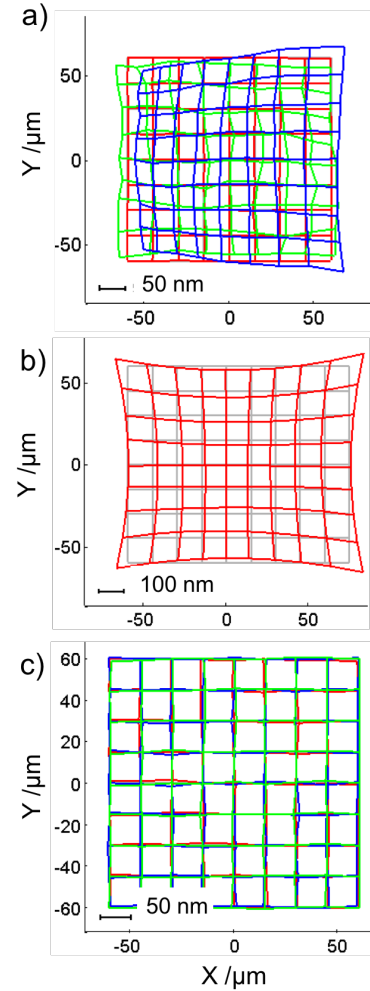


FIGURE 5. Self-calibration result. a) Input to the self-calibration algorithm and the placement scheme; b) calculated distortion of the CSI system with 50 $\times$ objective lens; c) overlay of the distortion-corrected grids.

TABLE 2. Measured distortion and evaluation

Distortion /nm	Maximum value	x	y
RMS residual errors /nm	Reference view	2	2.2
	Translated view	2.4	2.1
	Rotated view	2.4	2.2

CONCLUSION

The method demonstrated in this paper provides a new methodology for lateral distortion correction in 3D optical imaging systems. Unlike the traditional method, in which the distortion

correction has to rely on some precision-manufactured and calibrated standard artefact with structured patterns, the new approach makes use of an arbitrary surface that can be easily found, e.g. a coin surface. A precision of a few nanometres can be achieved for the distortion correction over a large FOV. The spatial sampling of the distortion is no longer limited by the structures in the artefact. A rarely observed drift due to the motion of the axial scanner or the translation stage in CSI has also been found, and the drift is of the order of a few tens of nanometres. Although an absolute scale is still needed to make the calibration traceable, the problem of obtaining the traceability is simplified as only an accurate measure of the distance between two arbitrary points is needed. Thus, the total cost of transferring the traceability may be reduced significantly using the new approach. In the future, a new method that may simplify the way of obtaining absolute scale will be investigated, and a 2.5D self-calibration method will be developed for correction of field curvature effects in CSI.

ACKNOWLEDGEMENTS

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CHARACTERIZATION AND OPTIMIZATION OF ULTRA-PRECISION MACHINE TOOLS AND PROCESSES BY TIME RESOLVED PSD SIGNATURE OF MICRO/NANO STRUCTURED OPTICAL SURFACES

Eckart Uhlmann^{1,2}, Stefan Kühne¹, Marco Jagodzinski¹, Markus Malcher¹

¹Institute for Machine Tools and Factory Management IWF
Technische Universität Berlin
Germany

²Fraunhofer Institute for Production Systems and Design Technology IPK
Germany

INTRODUCTION

Since its independent development by Rittenhouse [1] and Fraunhofer [2], the production of diffraction gratings has been a limiting area of precision. This is a constant motivation to increase the precision of manufacturing processes. Important milestones were the development of the first mechanical ruling machines [3], Michelson's idea of the production of gratings by interference lithography [4], and the development of ultra-precision machines.

Regarding today's production of diffractive optical elements (DOE), a plurality of, in some cases, competing production processes exists. In particular, there is a competition between lithographic and mechanical processes. Due to their specific advantages and disadvantages, each process is qualified for a specific range of versatile applications [5]. Blazed gratings (FIGURE 1), for example, offer the theoretic possibility to achieve particular high diffraction efficiency.

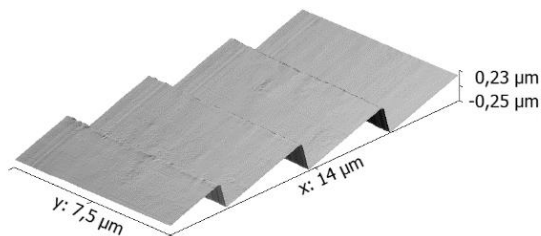


FIGURE 1. Exemplary grating structure

In practice, these theoretic benefits can only be achieved by highly specialized staff. Due to the developments of the last decades, the lithographic processes are established and cover

a large part of the diffraction grating market segment. A comprehensive description of the established production methods is provided by Glaser [6]. However, further developments of the multi axis UP technology in recent years enable the flexible production of specialized diffraction gratings and expand the possibilities of market coverage. For example, miniaturized concentric designs such as Offner spectrometers can be realized by highly curved gratings (FIGURE 2).

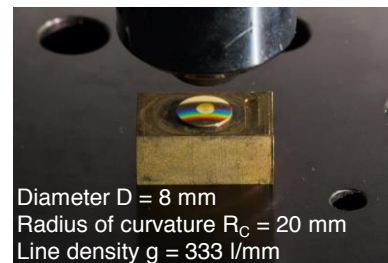


FIGURE 2. Exemplary Offner-grating

Ultra-precision machinery is the most accurate class of machine tools. The achievable precision is nowadays not limited by the available measuring technology, but rather by disturbance variables. The disturbances result primarily from thermal effects and vibrations as well as pulsations. Particularly in the case of the production of diffraction gratings with a structural height $h < 100$ nm, the ultra-precision machining gets to its limits. The challenge is growing with regard to the production of diffractive structures on curved surfaces [7,8]. Due to the necessary degrees of freedom and the number of involved machine axes the achievable spatial accuracy is reduced [9]. This limit can be shifted by means of targeted measurement and an extended knowledge of the disturbance variables.

Thermal effects generate shape deviations with low spatial wavelengths of several microns, a shape deviation up to $\text{RMS} > 100 \text{ nm}$ and limit the absolute accuracy. Regarding shaping processes, vibrations are transmitted into the work pieces by the tool and generate ripple of usually $\text{PV} < 20 \text{ nm}$ with a mid/high spatial frequency in the range of $k < 0.125 \mu\text{m}^{-1}$ (cutting speed $f_c = 500 \text{ mm/min}$) and essentially limit the achievable minimum structure size. With smaller structures, the importance of vibration reduction increases. The causal clarification of the ripple is often not trivial, but can be supported by the use of PSD (power spectral density). The actual resulting spatial frequency caused by vibration is directly related to the hertzian frequency depending on the cutting speed. A computation of a time resolved PSD from the usually spatial resolved PSD simplifies the causal clarification. Each machine has a very specific PSD signature, which can be determined by shaping processes and WLI (white light interferometry) measurement. Changes to this specific signature allow the detection of machine faults. Using the example of blazed grating structures, it is shown that this method is a powerful tool for the evaluation and optimization of ultra-precision machines. The surface-dominant frequency bands can be assigned to mechanical and electromagnetic sources, which can be eliminated in some cases. In order to eliminate mechanical and electromagnetic vibrations, the method is explained on the basis of selected examples. The remaining time resolved PSD signature of a machine enables statements on the suitability of the machine for the production of specific optics. In addition, the knowledge about the frequencies gained from the time resolved PSD can also be used to shift the spatial frequencies by use of an appropriate cutting speed. The experiments represent the current limitations and possibilities of the production of imaging diffractive optics by ultra-precision machining.

MACHINING SETUP

All experiments were carried out on an ultra-precision machine LT-Ultra MMC1100. The machine was modified with regard to the production of diffraction optics. As shown in FIGURE 3 those modifications include active and passive measures for the reduction of thermal and mechanical disturbances. Regarding long-term stabilization of temperature in the range of $\vartheta = 20^\circ\text{C} \pm 0.015^\circ\text{C}$, the machine is located in a specially developed temperature chamber. A

reduction of vibration was achieved by active vibration decoupling and stiffening of the machine. In addition all pulsation sources are removed and the machine is placed on a foundation which is decoupled from the building. Six machine axes are available, two lateral axes (X, Y), two rotatory axes (B, C) and two vertical Z-axes.

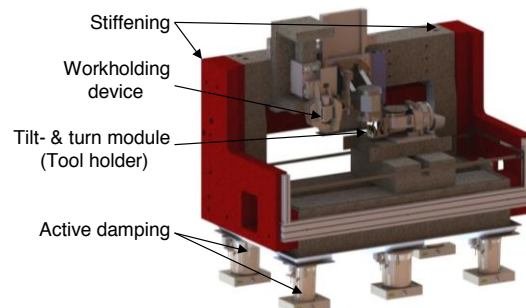


FIGURE 3. Experimental machine tool

The machine system is able to operate with five axes simultaneously. Regarding the production of imaging diffraction optics a four axis process is used. Therefore the possible axis configurations X-Y-Z₂, X-Y-B-Z₂, X-Y-Z₁ and X-Y-B-Z₁ result. All axes are positioned by glass scales in closed loop control with a repeatability of $\sigma \leq 25 \text{ nm}$. In order to reduce kinematic errors, self-developed mathematic methods based on indirect measurements are used [10]. The resulting compensation models are implemented in the CNC-code. Depending on the machine configuration a ripple remains in the shaped structures (FIGURE 4), despite the modifications. Because the ripple results from machine vibration, it is usually not in phase for the single grooves.

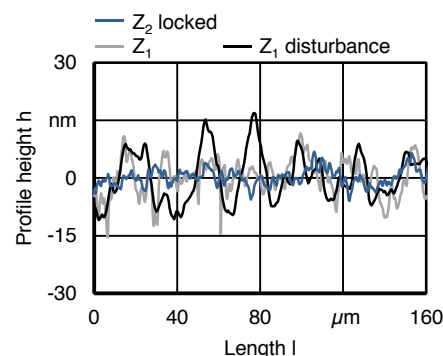


FIGURE 4. Exemplary roughness crosscuts

Due to this, height deviations occur in the structure cross-section. Those deviations lead to shifting phase relations of the incident radiation.

Depending on the wavelength of the incident radiation, errors in the diffraction spectrum occur. With decreasing wavelength λ , the acceptable structural error decreases too, depending on the application.

MEASURING AND VALUATION METHODS

Vibration measurement and FFT

High-sensitivity inductive geophones SYNOTECH PCB-393B31 are used for the vibration measurement. The determined accelerations are used to calculate oscillation frequencies by fast fourier transformation (FFT). LABVIEW© (2016) is used for signal readout and FFT.

Vibration simulation

In order to improve the understanding of relationships between the machine vibrations (FFT) and the dominant spatial frequencies of the surfaces (PSD), the eigen modes (FIGURE 5) and related eigenfrequencies were determined simulated using SOLIDWORKS© (2016/17). The first mode is a vibration with orientation in the Y-Z-plane of the machine, the second mode with orientation in the X-Z-plane. The 3rd mode is a torsional vibration.

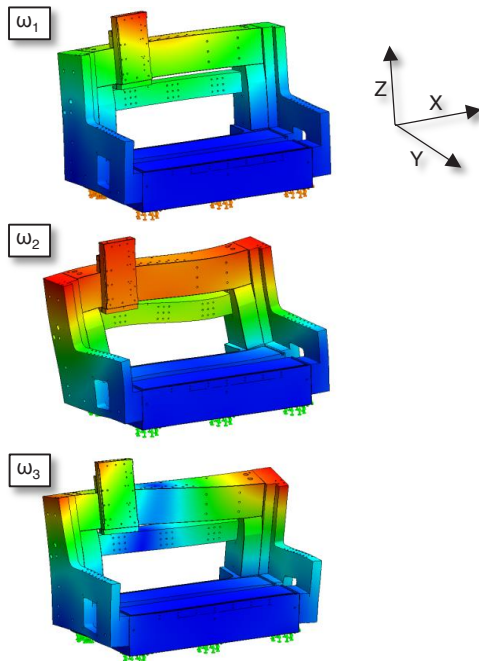


FIGURE 5. Oscillation simulation

Due to the orientation, the first mode is to be considered as particularly critical. The resulting ripple amplitude Δh ($5 \text{ nm} < \text{PV} < 20 \text{ nm}$) of the

structures is defined by the distance d of the tool engagement point from the oscillation axis. It is linearly scaled with the distance (FIGURE 6). Ideally, the tool would be close to the oscillation axis. Depending on the necessary machine setup the minimum distance is limited.

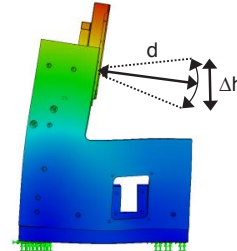


FIGURE 6. Relation between distance d and ripple amplitude

Structure measurement

The spatial wavelengths of the surface defects caused by the machine are in a range of up to several microns. In order to take a sufficient number of cyclic defects into account, a WLI (Type: ZYGOLOT NEWVIEW 5010 objective 20x, $\text{NA} = 0.4$) at a measurement area of $175 \mu\text{m} \times 131 \mu\text{m}$ and a spatial resolution of $\delta = 880 \text{ nm}$ is used. The open source program GWYDDION© (2.44) is used to evaluate the measurement data and generate the PSD's. A further processing of the data and the generation of time-resolved visualizations is carried out using MATLAB© (2016).

PSD calculation

According to Duparré [11] the PSD-function, which provides a clear description of the surface, can be calculated with EQUATION 1 as absolute square of the fourier spectrum of the surface topography.

$$\text{PSD}(f_x, f_y) = \lim_{L \rightarrow \infty} \frac{1}{L^2} \left| \int_{-L/2}^{L/2} \int_{-L/2}^{L/2} h(x, y) \times e^{-2\pi i(f_x x + f_y y)} dx dy \right|^2 \quad (1)$$

The PSD calculation generates information about the sagittal as well as the tangential structure. Regarding diffraction gratings, statements about roughness, dominant spatial frequencies and about grating structure are enabled. If the relevant topographies are orthogonal to one another, as it is for DOE's, the PSD can be discretized to cartesian coordinates by EQUATION 2.

$$\text{PSD}(f_x)_N(m) = \left| \left(\frac{T_0}{N} \right) \sum_{n=0}^{N-1} h(x)_n \times e^{-i2\pi mn/N} \right|^2 K(m) \quad (2)$$

With regard to the shaping process, the dominant spatial frequencies caused by machine vibrations depend on the cutting speed f_c , which corresponds to the speed of the axis. An increased cutting speed leads to a decreased spatial frequency. The process specific $\text{PSD}(k)$ can be shifted as shown in FIGURE 7.

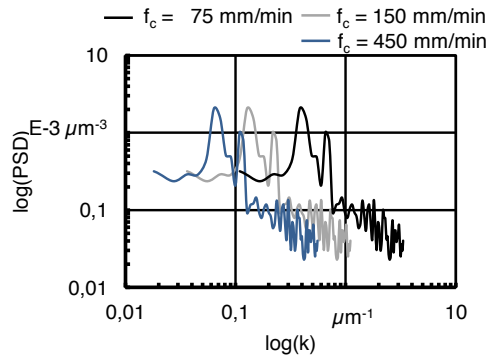


FIGURE 7. Calculated $\log(\text{PSD}(k))$ (sinusoidal interference)

Considering the cutting speed, the resulting frequency related $\text{PSD}(k)$ is converted into a hertzian frequency related $\text{PSD}(t)$ by EQUATION 3, in order to obtain machine specific PSD's. Instead of the common double logarithmic presentation of the $\text{PSD}(k)$ a intuitive linear presentation is used for the $\text{PSD}(t)$.

$$f = \frac{k f_c}{2\pi} \quad (3)$$

Those $\text{PSD}(t)$'s are almost congruently for different parameterizations. The amplitude changes less than 2% in the area of the relevant cutting speed. However, in order to ensure comparability, a cutting speed $f_c = 550 \text{ mm/min}$ (chip thickness $h_z = 1 \text{ μm}$) is used for all the experiments.

In the case of machines with a variety of axes, each axis configuration may have its own PSD signature. A compromise of possible degrees of freedom and resulting PSD function allows the selection and optimization of the most suitable configuration, which will be explained by selected results and examples.

RESULTS

Configuration including Z_2

Regarding the production of planar DOE's the configurations X-Y- Z_2 or X-Y-B- Z_2 can be used. Configuration X-Y-B- Z_2 is used for the production of planar varied line space gratings (VLS). In order to suppress the influences of the closed loop control, axis Z_2 offers the possibility of a mechanical locking. A resulting PSD in combination with a vibration FFT and simulation results is shown in FIGURE 8.

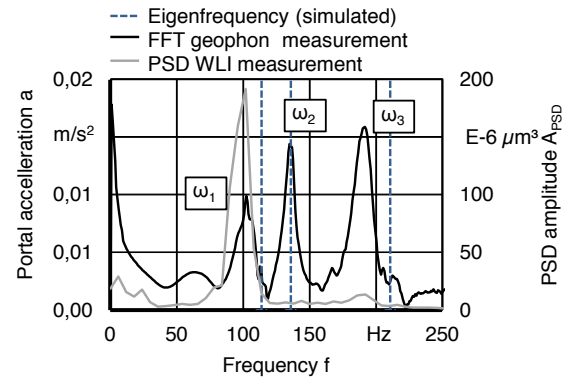


FIGURE 8. Comparison of FFT, $\text{PSD}(t)$ & FEM

The comparison of the acceleration FFT and the time-resolved $\text{PSD}(t)$ of the srface (in machining direction) shows local maxima at identical frequencies. As expected, the maximum peak of the PSD is in the range of the first machine eigenfrequency. However, no PSD maximum is found in the range of the second eigenfrequency, because the orientation of the oscillation is identical to the machining direction. Regarding the 3rd eigenfrequency, it can be seen that the torsional vibration recurs with a small PSD amplitude. Therefore, the spectrum is a signature of the used machine.

The same is true regarding other configurations as shown below. The influence of the B-axis is not significant compared to the portal oscillation; for this reason, the PSD's of the configurations are almost identical.

In addition, the PSD's of the configuration X-Y- Z_2 , before and after machine portal modification, are shown in FIGURE 9. The characteristic signature of the PSD remains. The stiffening of the portal has caused a shift of dominant frequencies and thus a significant amplitude reduction of the ripple.

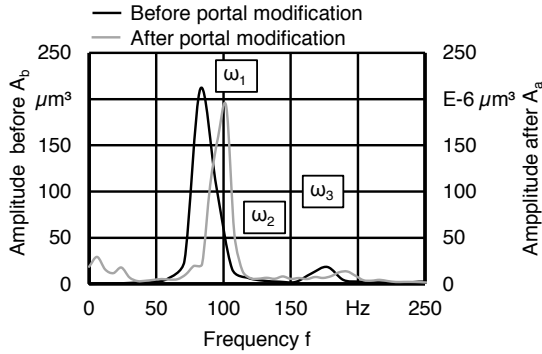


FIGURE 9. Machine portal modification PSD(t)

Configuration including Z₁

Regarding the production of curved diffraction optics, an increased number of degrees of freedom is required. Depending on the grating specifications, this leads to the configurations X-Y-Z₁ or X-Y-B-Z₁. Characteristic PSD(t) spectra of those configurations are shown in FIGURE 10 on the example of planar gratings. The maximum peak is identical to the Z₂ configurations. The influence of the B-axis is also not significant. Noticeable is the additional peak at approx. $f = 50$ Hz, which will be explained on the example of curved gratings.

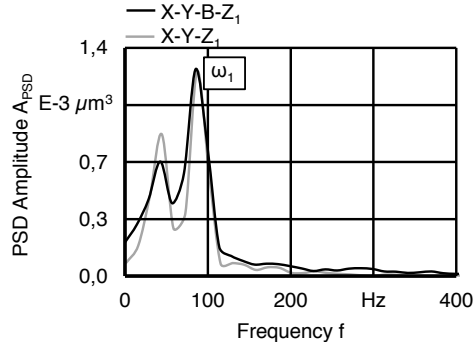


FIGURE 10. Planar grating PSD(t) (Z₁-axis)

Practical example 1: mechanical interference

Identically to the spectrum of the planar optics, the peak at $f = 50$ Hz remains in the production of curved DOE as shown in FIGURE 11. With dynamic operations of Z₁, this additional vibration causes an growing amplitude of the $f = 100$ Hz oscillations. Due to this excitation of the machine portal, an unacceptable ripple up to $PV = 20$ nm occurs. At a frequency of $f = 50$ Hz, the assumption of electromagnetic oscillation induction is legitimate. This can be a vibration generated by magnetostriction of a transformer for example. Since all transformers are decoupled from the machine, a peak from an

electromagnetic oscillation would be more narrowband in both the FFT and the PSD and independent of the dynamic behavior. The oscillation source is mechanical.

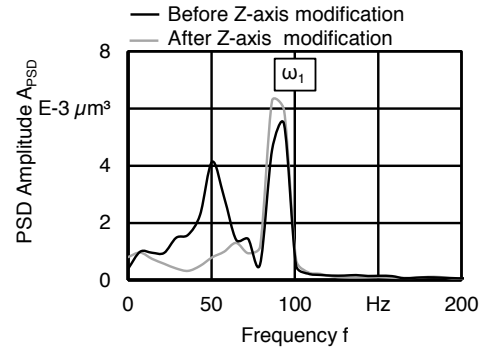


FIGURE 11. Spherical grating PSD(t) (Z₁-axis)

This comparatively low-frequency oscillation (below the first machine eigenfrequency) can solely be caused by a low stiffness system. In this case, the source was located in the weight compensation gear of the Z-axis, which is a pneumatic system whose oscillatory support is excited by the dynamic movement of the Z₁-axis. A stiffening of the support eliminated the low oscillation frequency. The effectiveness of this measure is comparatively shown in FIGURE 11. The $f = 50$ Hz peak is eliminated.

Practical example 2: electrical interference

Deviations from machine specific frequencies are to be classified as disturbances. In FIGURE 12, the effect of another type of disturbance is shown.

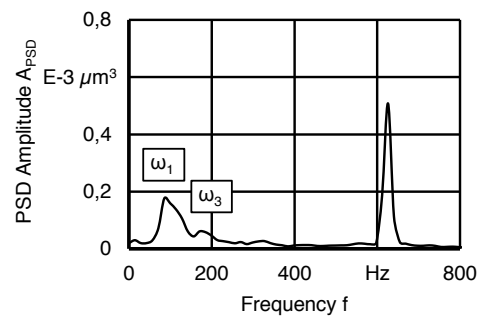


FIGURE 12. Electrical interference

In addition to the known peaks, another peak, characterized by a frequency of $f > 500$ Hz, occurs. The affiliated structure cross-section has a perfectly sinusoidal profile. In addition, the ripple ($PV = 15$ nm) is strikingly phase-identical for the single grooves. This comparably high frequency, in addition to the high amplitude as well as the identical phase, is atypical for a

mechanical oscillation. Therefore, it has been suggested that it is an electronic or electromagnetic disturbance. This assumption proved to be true. In that case an electrical grounding loop induced a signal into the closed loop control and caused an oscillation of the B-axis. Similar high-frequency disturbances can be caused by contaminated glass scales or process forces which influence the closed loop control inadmissible. FIGURE 13 shows such a disturbance with a frequency of $f = 1500$ Hz.

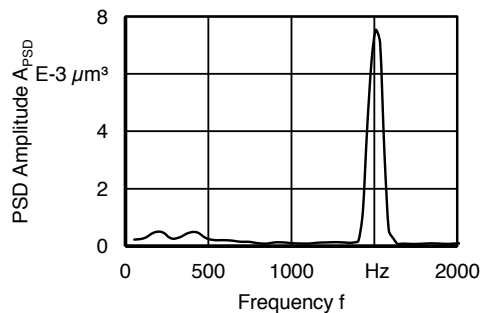


FIGURE 13. Closed loop control interference

CONCLUSION

The analysis of shaped grating structures is an excellent procedure to evaluate ultra-precision machines. A correlation of different information was used to clarify the direct causal relationship between specific machine oscillations and resulting structure cross-sections. The clarification is simplified by the use of time-resolved PSD. Regarding diffractive structures, it has been shown that the production of efficient blazed gratings, besides thermal effects, is limited by the machine eigenfrequencies. Using selected examples it has been proven, that there is a characteristic PSD signature for each machine and configuration. A change of this signature indicates an induced disturbance. Resulting significant PSD spectral components, which are independent of the eigen modes, were eliminated. Regarding the experimental machine setup, the ripple and therefore the local phase error could be reduced to a range $PV < 5$ nm for planar DOE and $PV < 10$ nm for curved DOE.

ACKNOWLEDGEMENT

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during the experiments. Felix Weiß is to be mentioned in particular.

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Surface Slope Error distribution on precision optical surfaces

Luc Girard¹, John Kincade¹, and Lou Marchetti¹
¹ZYGO Corporation, Extreme Precision Optics (EPO)
Richmond, California, USA

INTRODUCTION

Specification of slope and mid-spatial frequency error for optical surfaces is now frequently done with a comprehensive understanding of their impact on image formation. Ignoring the potential impact of these errors may have dire consequence on the optical performance [1]. This presentation will bring to focus the intrinsic limitations of optical surface finishing for extreme precision optics with respect to achievable slope errors.

ZYGO EPO has delivered numerous extreme precision optical components and systems to customers over the years. Applications range from space missions, scientific endeavors and commercial products. Empiric data of a sample of 3 of the finest optical surfaces produced by ZYGO EPO will show the distribution of surface error with spatial frequency in the form of Power Spectral Density (PSD) graphs. We will explore the slope error distribution with frequency of those sub-nanometer surfaces to determine what might be the practical limit on specifying slope as a functional performance indicator.

OPTICAL SURFACE DESCRIPTION

We have extensive metrology of the surface of 3 noteworthy surfaces produced by ZYGO EPO. It goes without saying that these optics have performed exceptionally well in the field.



FIGURE 1. MET5 Lithography system's M2 Aspheric mirror with EUV Multilayer coating.

MET5 M2 mirror

The first one is a 280mm diameter aspheric mirror shown in Figure 1. This low-expansion glass ceramic mirror, called M2, is produced as part of the MET5 EUV lithography small field 0.5NA exposure tool to be used at 13.5nm wavelength [2][3]. The mirror is part of a system that achieved transmitted wavefront error of less than 0.25nm RMS, and is intended for printing at resolution of less than 8nm [4]. The achieved figure error for this surface is only 0.04nm RMS and the RMS integrated over the entire range from Clear Aperture (CA) dimension down to 10nm spatial periods is 0.14nm RMS.

LIGO Input Test Mass (ITM)

The second is an Advanced LIGO Input Test Mass (ITM) optic. Advanced LIGO is now famous for the recent detection of gravity waves [5]. The optic is a massive 340mm diameter by 200mm thick piece of high quality fused silica and very demanding specifications [6].



FIGURE 2. A LIGO ITM Optic during measurement on the Phase Measuring Microscope.

The surface is spherical with a radius of curvature of 1.929km and its critical aperture is 160mm. The achieved figure is 0.08nm RMS after tilt and power have been removed. and the RMS integrated from CA dimension down to 0.001mm is 0.12nm RMS.

X-Ray Beamline grating substrate

The next optic is a rectangular mirror made of monocrystalline silicon used as an X-Ray beamline grating substrate for the National

Synchrotron Light Source II (NSLS-II) at Brookhaven National Laboratory (BNL). The mirror is flat with a rectangular clear aperture of 155mm x 30mm. The achieved RMS figure over the CA is 0.174nm RMS. The integrated RMS of all the errors with spatial periods ranging from CA down to 10nm is 0.19nm RMS. It's tangential slope error was measured with two independent devices at an incredible 39 nanoradians RMS [7].

The common point of all these optics is very stringent requirements over an extended range of spatial frequencies to control an aspect of performance required for the application. That could be light scattering for throughput, or scattering onto the image plane, or control of wavefront slope for high fluence considerations.

OPTICAL SURFACE MEASUREMENT

Measuring optical surfaces over an extended spatial frequency range requires several instruments and configurations to ensure proper coverage. The instruments available to measure the surface are: Phase Measuring Interferometer (PMI), Sub-Aperture Surface Height Interferometric Measuring Instrument (SASHIMI), Phase Measuring Microscope (PMM) or Optical Surface Profiler used with various objectives and an Atomic Force Microscope (AFM) with 2 different scan lengths. SASHIMI is a custom built sub-aperture stitching instrument designed to bridge the gap between figure metrology and PMM required for parts with demanding Mid-Spatial Frequency Range (MSFR) specifications.

Power Spectral Density (PSD) Curve

With sufficient coverage and adequate overlap of the instrument transfer functions. All the measurements are combined to create a single PSD curve that represent the surface error with fidelity. The combination process uses only valid instrument responses and accounts for instrument characteristics like detrending, sampling limit and detector noise.

The resulting PSD curves help evaluate the surface error in specific spatial frequency ranges that are independent of instruments. The slope and level of the PSD are often used to specify the distribution of error of a surface. First introduced for the National Ignition Facility optics to control uniformity of the wavefront and avoid concentration of energy along the beam path due to mid-spatial frequency error and

associated slope [8], the PSD method is now accepted and part of the ISO 10110 specification. [9]

Figure 3 shows the PSD curve for our 3 optical surfaces of interest. A curve shows a typical NIF specification line and an area where precision optics PSD curves will typically be found. The 3 extreme precision surfaces can be approximately described using the NIF method with coefficients $A=0.001\text{nm}^2\text{mm}$ and $B=-1$.

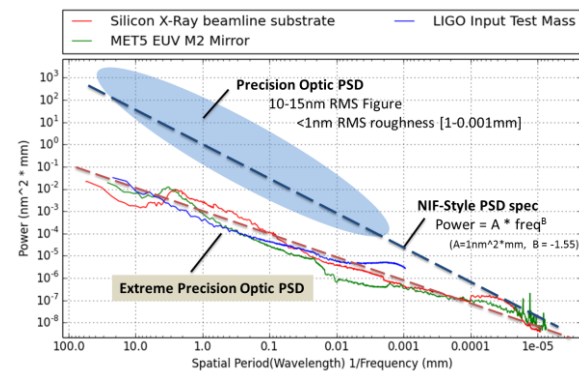


FIGURE 3. Power Spectral Density (PSD) graph showing 3 extreme precision optics curves. The typical location of standard precision optics PSD curves is shown in blue in the graph. A NIF-Style curve with $A=1\text{nm}^2\text{mm}$ and $B=-1.55$ is also shown for reference.

SLOPE FROM EXTREME PRECISION OPTICS

The surface error in spatial period decades is reported in Table 1. The amount of error in each of the decades for extreme precision optics varies around a value of 0.05nm RMS. Such low level of error over the entire frequency range is extremely difficult to achieve. This level or error is at the limit of state of the art polishing methods for these 3 different materials, and approaching the noise level of the phase measuring instruments. The RMS error in Table 1 does not decrease as the spatial frequency increases, as one would imagine given the lower level of the PSD curve at this end of the frequency range.

The RMS slope of the surface error can be obtained for the same ranges used for Table 1. The result of the slope estimation is shown in Table 2. The RMS slope value essentially increases by an order of magnitude for each of the rows of the table, matching the reduction in the lateral scale of the sampling.

TABLE 1. Integrated RMS surface error in different spatial period ranges. The units for surface error is nanometer RMS.

Spatial Period Range, mm	Integrated surface error, nm RMS		
	MET5 M2	LIGO ITM	X-Ray Optic
10-1	0.062	0.040	0.062
1-0.1	0.068	0.030	0.031
0.1-0.01	0.035	0.035	0.023
0.01-0.001	0.033	0.064	0.021
0.001-0.0001	0.040	--	0.037
0.0001-0.00001	0.067	--	0.057

Surface error slope depends on two factors: the surface error height and the characteristic lateral scale of the error. From the data in Table 1 and Table 2, the slope of extreme precision optics is clearly driven by the change in the lateral scale rather than the surface error height. For lesser precision parts, this may not always be the case. For those optics, the slope coming from surface height error may contribute significantly to the surface slope.

TABLE 2. RMS surface error slope for the same ranges as Table 1. Slope units is microradians.

Spatial Period Range, mm	RMS Surface error slope, microradians		
	MET5 M2	LIGO ITM	X-Ray Optic
10-1	0.23	0.11	0.18
1-0.1	1.72	0.89	0.80
0.1-0.01	8.9	10.6	5.7
0.01-0.001	96.2	235.7	76.8
0.001-0.0001	1258.1	--	1287.5
0.0001-0.00001	14433.2	--	17566.4

Slope from Surface error maps

The trend observed from Table 2 can be confirmed by looking at the RMS slope value for various instrument maps. Figure 4a) shows surface error maps for figure, PMM and AFM data and in Figure 4b) the corresponding surface slope maps.

The surface slope maps show slope values that increase dramatically as the spatial frequency of the data increases. Even looking at any individual slope map, one can see that the slope values are essentially dominated by the slope of

the highest spatial frequency error present in the measurement data.

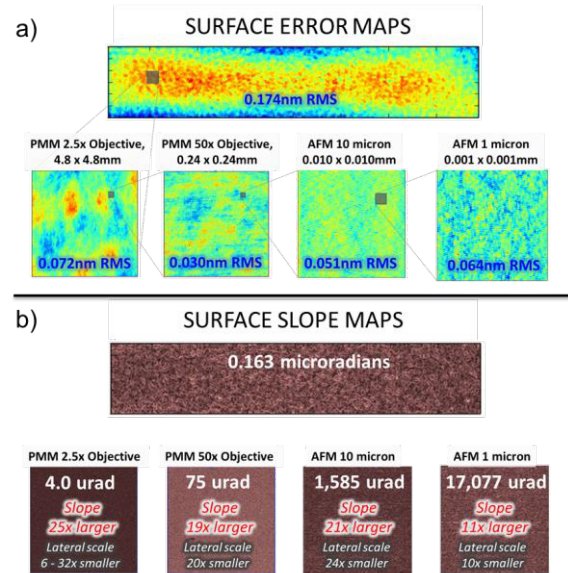


FIGURE 4. X-Ray beamline optic (a) Surface Error maps for various instruments. The small gray box shows the size of the sampling area of the next instrument used to measure the surface. (b) Surface slope maps for the corresponding maps show an increase in slope value for each instrument inversely proportional to the lateral scale of the data.

Slope from PSD data

The PSD shown in Figure 3 relates the amount of error on the surface of an optic to the spatial frequencies of the error. This information is sufficient to characterize the slope present at the surface of an optic, in a statistical sense. In the Fourier domain, the error at a given frequency can be thought of as a sine wave, which has slope characteristics that are well defined. Obtaining an estimate of RMS slope per unit spatial frequency from the PSD is therefore possible. A PSD of surface error slope derived from the PSD of surface error is presented at Figure 5. As one could have guessed based on the slope evaluation from surface maps, the curves show an increase in the slope power as the spatial frequency increases for the extreme precision optics. The NIF style curve defined by a power law also exhibits the same behavior. In fact, the slope of the curves of the PSD slope are related to the slope of the standard PSD. the NIF coefficient B of the PSD of surface error becomes B+2 for the PSD of surface slope error.

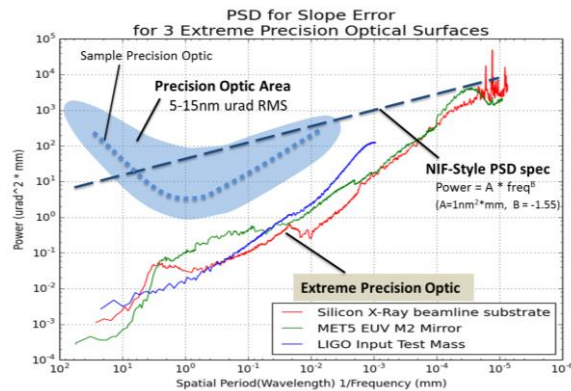


FIGURE 5. PSD of surface error slope derived from the PSD of Surface error presented at Figure 3.

For extreme and high precision optics, the PSD for slope error does not deviate much from a straight line in the log-log graph, but this is not always the case. For precision and standard optics, the figure error can contribute significantly to the slope error, as indicated by the banana-shape blue area and the sample PSD curve in Figure 5. For such optics, the slope from surface error with characteristic dimension of a millimeter-scale and greater is dominated by figure error. Below a millimeter scale, the slope error PSD will transition to the regime where the lateral scale dominates and the curve will follow an upward trend.

CONCLUSION

Surface slope error and mid-spatial frequency errors are specified to control several optical performance parameters. Whether to control geometric image formation through slope specification, or to limit the quantity of scattered light from diffraction that impacts the contrast or flare in optical systems [10], specifying surface slope must be done with awareness of the sharp increase in the slope value associated with small sampling lengths.

A study of the slope of the best surfaces available confirmed that the RMS slope value at high spatial frequency is more significantly affected by the frequency of the error than by the magnitude of the surface error height. Figure 6 shows a practical limit of slope for super-polished surfaces obtained from the PSD graph of RMS slope shown in Figure 5. When specifying accumulated RMS slope error at small sampling lengths, certain slope limits must

be respected to create realistic and cost-effective slope specifications.

Estimates of those practical limits are shown in Figure 6 below. When evaluating slopes with sampling period as low as 1mm, it will be very difficult to produce surfaces with RMS slopes less than 0.22 microradians. When the cutoff period is further reduced to 0.1mm, the RMS slope drastically increases to 1.71 microradians. Sampling down to even smaller scales will see the slope exponentially increase to reach tens of milliradians at sub-micron sampling.

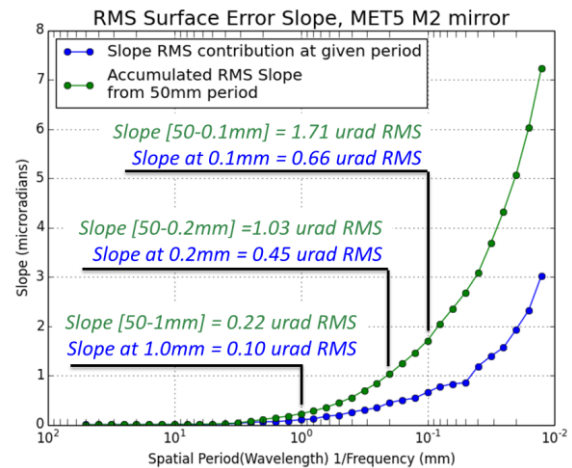


FIGURE 6. RMS slope error for the MET5 M2 mirror surface. The blue curve represents the RMS error at a specific period and the green curve is the RMS slope accumulated from 50mm spatial period down to the specified spatial period.

The idea of a PSD for slope error has been introduced and limits for extremely good surfaces have been found. Studies of a variety of parts with this method will be required to determine if this is going to be a good tool for specifying slopes.

As a corollary, the slope PSD method highlights the spatial limitations of even super-smooth surfaces to support very small instantaneous apertures on those surfaces without significant degradation to optical performance. Further, the slope PSD and slope limitation curves in Figures 5 & 6 above can be practically used to determine the minimum size limit of the instantaneous surface aperture given a desired optical performance specification.

REVISITING PSD ANALYSIS CONSIDERATIONS

Katherine Creath^{1,2}

¹Optineering, Tucson, AZ, USA

²College of Optical Sciences
University of Arizona
Tucson, AZ, USA

INTRODUCTION

In the last two decades since the development and adoption of the new ISO10110 optical drawing standards [1], a larger number of optical surfaces are being specified with surface roughness as defined over a limited spatial frequency range. The advantage of this is that, in principal, it enables measurements by different instruments to provide comparable results. It also enables designers and fabricators the ability to target more than one spatial frequency range, or to ensure that artifacts from a particular fabrication process are mimimized.

It seems relatively straight-forward to make surface measurements with one or more spatial frequency ranges, back out instrument transfer functions, calculate the combined PSD (Power Spectral Density) data, and then integrate over the specified spatial frequency range. However, as I and others have discovered over the last two decades, there is no codified method for calculating the PSD, let alone for how to deal with instrument transfer functions and stitching.

I often get called on as a consultant to ensure that the customer is getting a surface that meets their specifications. It's not as easy as it seems to ensure that. Fabricators are not likely to give you the details about how they do their calculations. And interferometer makers don't want to give you the details of how their algorithms calculate a PSD. Plus, if you have to combine PSD's from multiple measurements or multiple instruments, it gets even more complex to get a satisfactory end result that all can agree upon.

My work on this topic began almost 30 years ago at Wyko when we were first developing PSD calculations. Over the years, I have had several clients ask for my advice in different calculations. What I present in this paper is the result of applying methods published in the literature as well as reverse-engineering curves

I've received from different vendors. In the next section, I present procedures for calculating 6 different 1D PSD calculations. These methods will be illustrated using a simple example to compare results. The paper ends with a short discussion.

What this paper does not cover are stitching methods, and methods of measuring and accounting for instrument transfer functions. We'll save those for another day.

METHODS OF CALCULATING PSD

To calculate the surface roughness (rms or Sq) of a surface over a specified spatial frequency range, we need to integrate the PSD between the limits of this range.

The 2D PSD

Rather than writing out this theory mathematically, the procedures will be outlined in pseudocode:

1. Read in the phase map
 - a. Account for aspect ratio
 - b. Transform to square pixels
 - c. For simplicity arrays have even numbers of square pixels for these calculations
2. Replace bad data point with NaN (not a number)
3. Remove Zernike terms
 - a. Make data zero mean
 - b. Replace outliers $>\pm 5X$ rms with NaN
4. Specify the clear aperture
5. Window the data
 - a. Gaussian window if circular
 - b. 2D Hann window if rectangular
 - c. Make sure edge of window coincides with edge of data aperture
6. Zeropad if desired
7. Calculate 2D PSD using

$$PSD_{2D} = \text{abs}(\text{fftshift}(\text{fft2}(\text{ifftshift}(\text{map}))))^2 / PSD_area$$

Where...

$$Spatial\ frequency\ interval = 1 / diam_PSD_array$$

Lowest spatial frequency = $1/\text{diam_PSD_array}$
Highest spatial frequency = $1/\text{pixel_spacing}$

8. Scale the PSD

- Area under the PSD = rms^2
- Because of Parseval's theorem this becomes

$$\text{rms_PSD_2D} = \sqrt{\text{sum}(\text{PSD}(:)) * \text{df}^2 / (4 * \pi^2)}$$

- The rms of the original data after terms removed and aperture applied is used accounting for bad pixels
- Each 1D or 2D PSD is scaled this way to integrate over spatial frequency and obtain the band-limited Sq (or rms)

Here is an example for a flat mirror measured in a Fizeau interferometer. All calculations performed in Matlab. FIGURE 1 shows the phase map as measured.

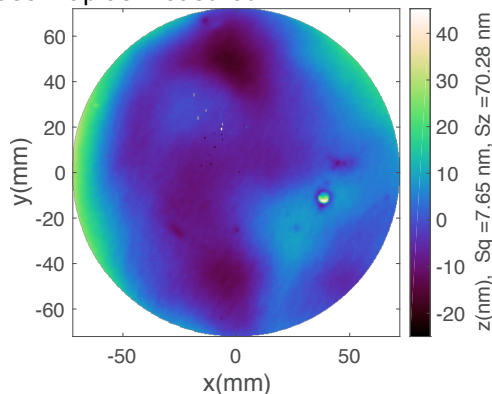


FIGURE 1. This phase map of a flat mirror has 496x496 pixels and was taken with a Fizeau interferometer. The artifact at 3 o'clock is a pit in the surface. The other missing data points (white dots at 11 o'clock) are from specs of dirt on the surface. These data get replaced by NaN (not a number).

After the Seidel Zernike terms (the first 8 "Fringe" terms plus the mean [2]) are removed, we are left with the residual phase map in FIGURE 2. FIGURE 3 shows the Gaussian damping window in the top plot, and the windowed phase data are shown in the bottom plot. NaN values have been replaced with zero (the mean value). Because of how the window-damping function works, the data are zero at the edges of the array. This will prevent ringing.

FIGURE 4 is a plot of the 2D PSD scaled so that the total power in the plot sums to the square of the rms of the data.

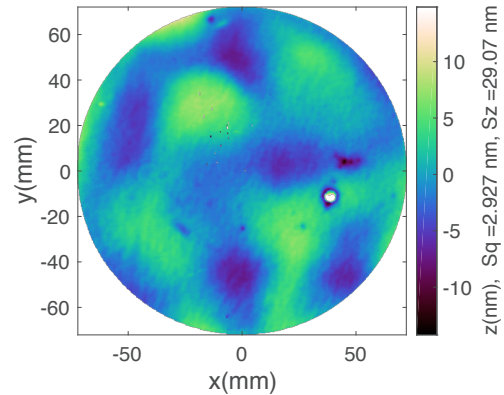


FIGURE 2. Residual phase map after the Seidel Zernike terms are removed.

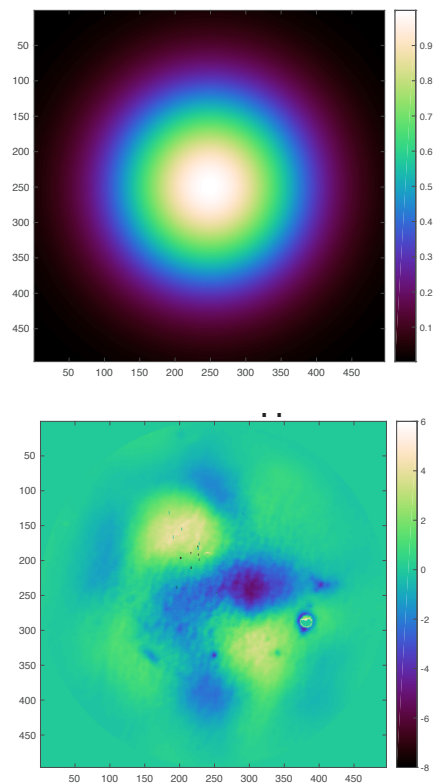


FIGURE 3. (Top) Circular Gaussian damping window function. (Bottom) Windowed residual phase data.

Methods of producing 1D PSD from 2D PSD

Now that we have a 2D PSD we can process this to find band-limited rms specifications. I want to point out that this is not something that's well-defined in practice. We could do this directly from the 2D PSD, but typically we look at 1D PSD plots. And therein is the crux. What's the best way to do this?

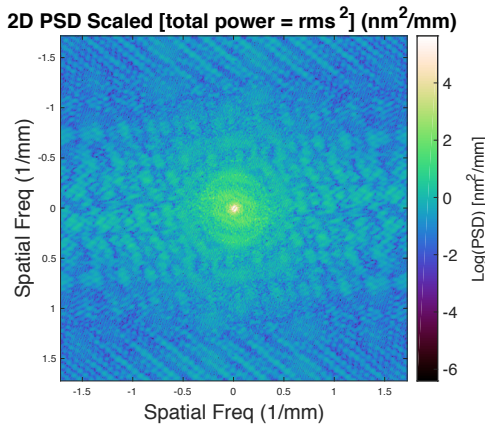


FIGURE 4. Two-dimensional PSD of windowed data in FIGURE 3. This has been scaled so that the total power = rms^2 of the residual data set in FIGURE 2.

After doing a literature search and talking to a lot of different vendors, I have found that there are as many different ways to do this [3-8]. I began to try the different published and stated methods on the same datasets, and found that they do not agree. No wonder we have difficulty comparing results. In this section, I will outline a number of different ways in which we can translate 2D PSD data into 1D PSD data.

Averaging rows (or columns) of 2D PSD

For this technique, after the 2D PSD is calculated, all the rows (or all the columns) of the 2D PSD are averaged and folded over so you see the positive spatial frequencies. The scaling is such that the integral (sum) under the 1D PSD plot equals the rms^2 (or Sq).

This technique has been in the Wyko/Veeco software for the last 3 decades. This method was developed for looking at surface roughness with an interferometric optical profiler (microscope) assuming you have a rectangular array of data.

Averaging 1D PSDs of each row (or column)

This provides comparable data to taking the 1D PSD of each row (or column) of data in the original dataset and then averaging all the rows together. Elson and Bennett outline this method in their work [3].

It needs to be noted that when you have data with a circular aperture, this may not be the best approach. However, it is still widely used in many current commercial software packages.

FIGURE 5 shows a linear Hann window used to dampen data when taking the PSDs of individual rows (Top) and a plot of the 1D PSDs of each row in an array.

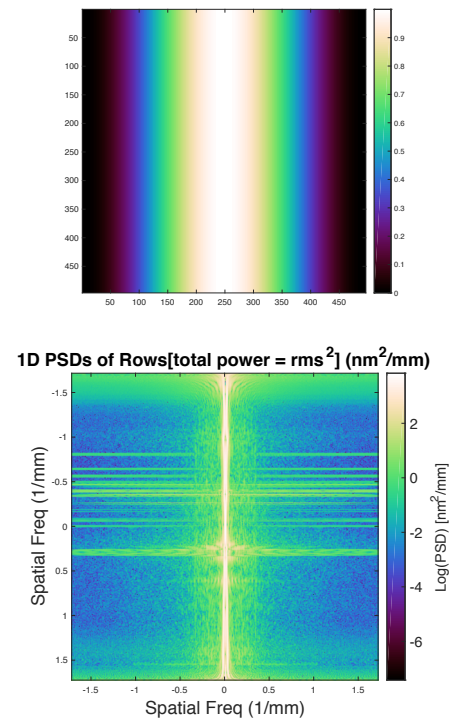


FIGURE 5. (Top) Linear Hann damping window function. (Bottom) 1D PSDs of each row of data in FIGURE 2.

FIGURE 6 shows 6 different types of 1D PSD plots, two of which I have described so far.

Fat rows of 2D PSD

Because of the inconsistency of the previous 2 techniques with circular apertures, Zygo in their MetroPro software [9] have been using “fat” rows for a 1D PSD estimate. Single rows of the 2D PSD are too noisy to provide good data, but average a few rows (or columns) near the center together and you get a better estimate of the PSD.

The example shown in FIGURE 6 averages 2.5% of the rows near the center (for a 496x496 array that's about 24 rows). This technique is more consistent between data sets that have circular or rectangular apertures. However, this estimate can end up missing some features depending upon orientation unless radial slices at different orientations are considered. It tends to be quite a bit noisier and provides a lower estimate than other techniques.

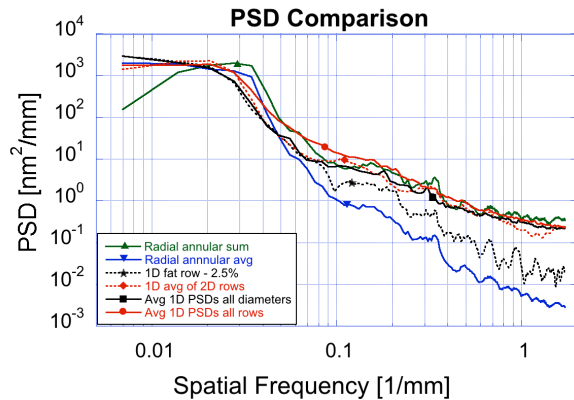


FIGURE 6. One-dimensional PSD plots of FIGURE 2 data set. This has been scaled so that the total power = rms^2 for each curve.

Radial integral of 2D PSD

This method appears in work from JPL [7]. They take a radial slice from the center to the edge of the 2D PSD and rotate it around the center to create an average of the values for each radius. This provides a much less noisy version of a slice out of the 2D PSD. It will average features that change with the azimuth, but it's good for circularly symmetric optics. In FIGURE 6 you can see that this estimate is much lower than the other techniques for higher spatial frequencies.

Radial sum of 2D PSD

Because of the issues with having a low estimate (and thereby likely underestimating the actual values) in the method above, this method using a sum rather than an average was developed [8]. It is another technique that is used for circularly symmetric optics with circular apertures. However, because there are so few points at low spatial frequencies, this estimate is not good at low spatial frequencies. At mid- and high-spatial frequencies it is comparable to the other techniques and tends to be higher at the high-spatial frequencies (see FIGURE 6).

Chordal PSD (1D PSDs of all Diameters)

This is an extension of a method published by CSIRO for measurement of the LIGO optics [5]. For circularly symmetric optics measured on a Fizeau interferometer, the PSDs of several chord diameters of the data set were calculated and then averaged together. The noise of course is reduced as more chords are calculated. Novak [4] suggested for the NIF optics that calculating the PSD for 4 chords at 45° could provide enough information about the

surface, but without averaging the PSDs of individual chords are noisy.

I propose extending this and calculating the PSDs of a large number of chordal diameters and then averaging them together. For this example, there are as many chords as there are rows. First the data are remapped so that each chord is on one row as shown in FIGURE 7 for the dataset in FIGURE 2. FIGURE 8 shows the PSDs of each chordal diameter in a row.

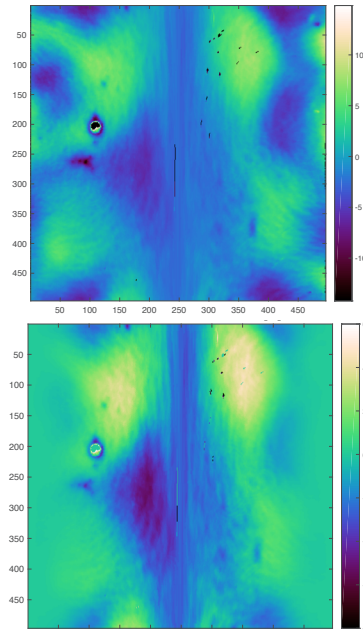


FIGURE 7. (Top) Each row is a chord at a different angle of the data set in FIGURE 2. There are as many chords as there are rows. (Bottom) After applying a linear Hann window.

In my opinion this method provides a more consistent PSD estimate for circularly symmetric optics than any of the other methods. It will average out any azimuthal structure just as the radial average and radial sum techniques will, but this method is more consistent across both low- and high-spatial frequency techniques when compared to those other 2 techniques, and therefore shows promise.

Zero Padding

Zero padding is another consideration when calculating PSDs. Adding more zero padding does reduce ringing and helps to reduce noise. FIGURE 9 shows the average of 1D chordal diameter PSDs for different amounts of zero padding. The FFT function in Matlab doesn't care whether the array is a power of 2 or not.

For these plots, no zeropadding indicates a 496x496 array. For zeropad = 512, the data are centered in a 512x512 array of zeros. For zeropad = 1024, and 2048, more zeros are added. It is noticeable that as you increase the zero padding you get more detail in the 1D PSD estimate. Of course there is a tradeoff with calculation time.

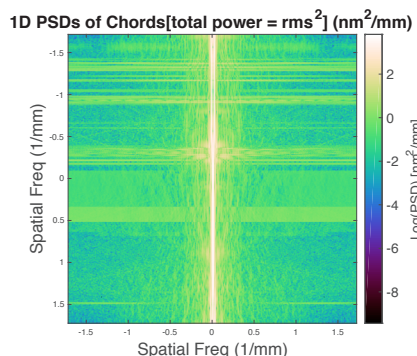


FIGURE 8. 1D PSDs of each row of data in FIGURE 7. Each row corresponds to a different chordal diameter of FIGURE 2.

DISCUSSION

It is obvious from the work presented that the choice in method used to calculate the 1D PSD makes a difference in the result and ultimately will change the outcome of a bandlimited surface rms (Sq) estimate.

There are many factors that need to be taken into account. Several of them have been presented here. The most obvious is choosing a method depending upon whether the data are circularly symmetric or have a circular aperture versus having a square or rectangular data. I have also shown how PSD estimates can change with zero padding. This study is not exhaustive in that I haven't presented other variables such as binning, terms removed, and filtering that can affect PSDs. There is still a lot of work to do in this area in order to help codify the best methods to use for these calculations. Choose carefully.

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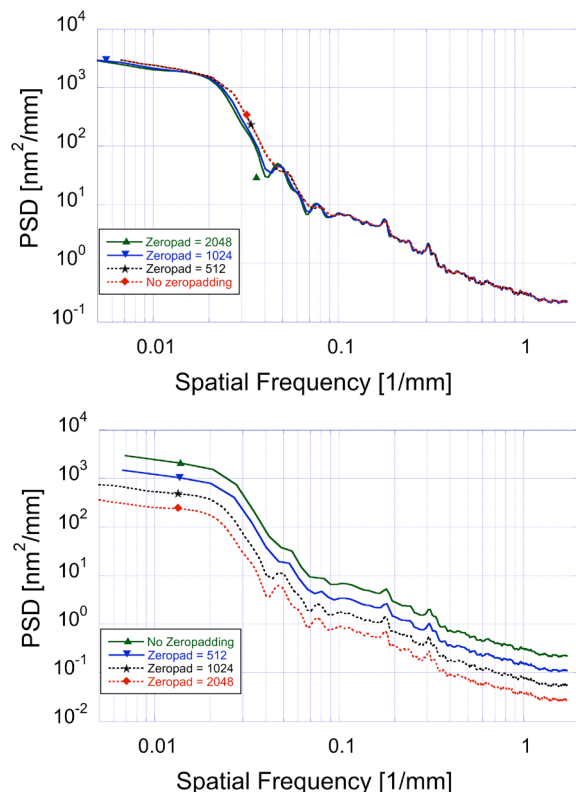


FIGURE 9. (Top) Average of 1D PSDs of all chordal diameters for different amounts of zero padding of windowed data from FIGURE 7. (Bottom) Same curves as top plots, but separated and no longer scaled correctly.

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COMPUTER GENERATED HOLOGRAMS AS 3D CALIBRATION ARTIFACTS

C. Zhou¹, and R. E. Parks²

¹Arizona Optical Metrology, LLC
Tucson, AZ 85743

²Optical Perspectives Group, LLC
Tucson, AZ 84750

BACKGROUND

Computer generated holograms (CGH) were developed in the early 1970's as an aid in the interferometric testing of optical surfaces¹. At the time the devices available for producing the CGHs were fairly low resolution plotting devices akin to the current ink jet printers. Over the years the means for making CGHs improved rapidly and they can now be made by e-beam methods with feature location precision of 10 nm rms or better. This high precision in patterning of the CGHs made it possible to create null optics for quite severe departures from sphericity, and CGHs have become a very practical method of testing aspheres with substantial departures from a sphere and for testing free form optics².

In this paper we step back from complex CGH patterns used to test aspheric and freeform optics to ask what can be done with the simplest CGH patterns and the high precision of pattern location on a photomask substrate. We first describe the use of patterns of equally spaced concentric circles to create an axis in space perpendicular to the CGH plane, and the Fresnel zone patterns that produce points in space when illuminated with a point source of light.

We then show how these points and axes are interrogated in a practical manor by means of an autostigmatic microscope (ASM), or for greater precision, with an interferometer. Following this discussion of how points and axes are created and retrieved from simple CGH patterns, we show how these features can be combined to produce artifacts for the calibration of precision instruments and the making of fixtures for the assembly of optical and mechanical components.

SIMPLE CGH PATTERNS AND THEIR PROPERTIES

It is well known that a pattern of equally spaced straight lines, a grating, will diffract light when the spacing of the lines is on the order of the

wavelength of light, and the angle of diffraction, α , is given by $\sin \alpha = m\lambda/s$ where m , an integer, is the order number and λ is the wavelength of the light and the light is normally incident on the grating. Now think of a pattern of concentric circles where the spacing of the circles is uniform, and envision a diameter of that pattern. It is a very narrow straight line diffraction grating.

From symmetry, a point source of light above the pattern but centered on it will produce cones of diffracted light, one cone converging and one diverging depending on the + or - 1st order as well as other cones of higher orders. For any practical distance above the pattern a bright spot appears surrounded by dimmer concentric rings when viewed with an ASM centered on the axis as shown in Fig. 1a or with an interferometer³ in Fig 1b. This pattern is not very efficient light wise so the farther from the CGH the brighter the source must be. However, a bright spot could be seen as far away as 10 m.

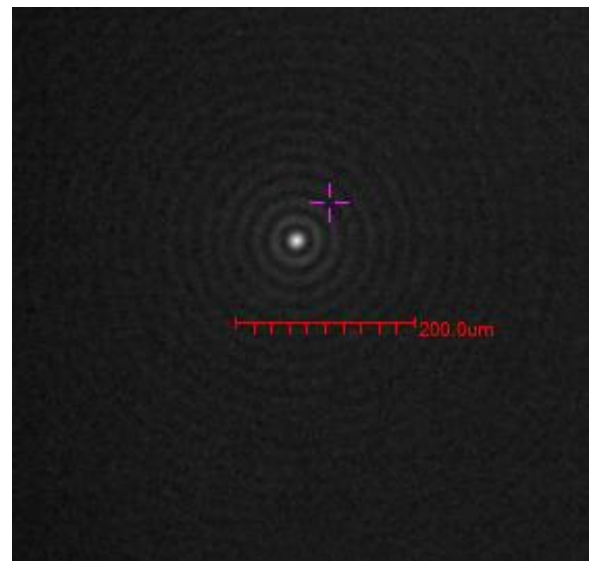


FIGURE 1a. Reflected spot produced by a CGH pattern of equally spaced concentric rings, or an axicon pattern.

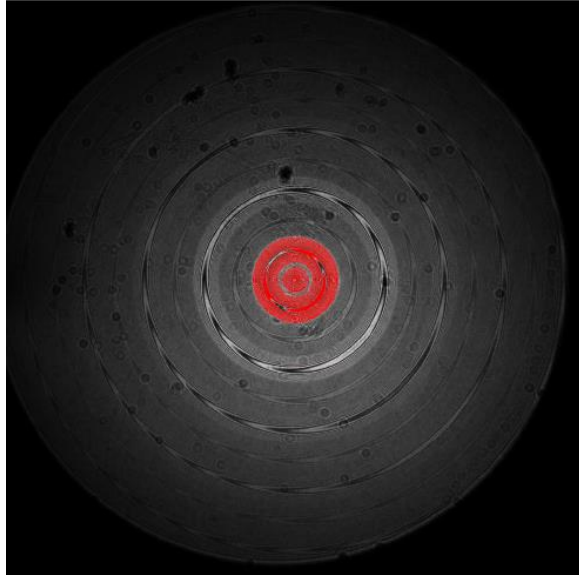


FIGURE 1a. Interferogram³ of light reflected from a CGH pattern of equally spaced concentric rings, or an axicon pattern. (The red in the center are saturated pixels.)

The Fresnel zones work much the same way as the equally spaced pattern but here the radii of the zones are proportional to the square root of the integer number of the ring in the pattern. If this pattern is illuminated by a quasi-monochromatic point source above and centered on the pattern the diffracted light will come to focus when the ring spacing $s = \sqrt{(R+n\lambda/2)^2 - R^2}$ where R is the distance above the CGH, n is the integer ring number and λ is the wavelength of the light in the point source. The reason the light comes to focus is that each ring reflects light back that is one wavelength farther away from the focus so all the light is in phase when it reaches the focus.

Since the ring spacing is proportional to R almost any distance above the CGH can be produced within practical limits of the e-beam writing. For example, for $R = 1$ mm a pattern with 100 rings would still have a ring spacing at the edge of the pattern of more than $1 \mu\text{m}$, and for $R = 10$ m a pattern with 100 rings would be about 25 mm in diameter although the f /number of the pattern would be very large ($f/400$) along with a large depth of focus.

The Fresnel zone patterns can be interrogated by either an ASM or interferometer. Typical results at the focus using either instrument are shown in Fig. 2. Using the ASM, in this case a

Point Source Microscope (PSM) with a red laser diode point source and a color camera shows the focused image from the Fresnel pattern, Fig. 2a. The image has a 4 point star shape because the CGH pattern was designed with a quarter wave of astigmatism to aid in finding best focus.

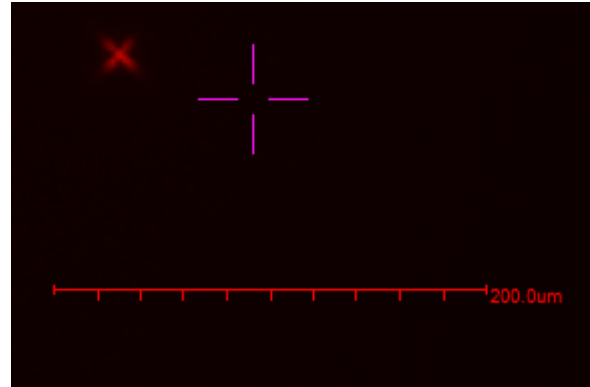


FIGURE 2a. Reflected spot from a Fresnel zone CGH pattern as viewed with a PSM with a 635 nm laser diode source. The cross in the red spot is due to a quarter wave of astigmatism built into the CGH Fresnel zone pattern to aid in focusing.

Best focus was found at 14.944 mm above the CGH and the pattern was designed to focus at 15 mm using light with a wavelength of 632.8 nm where the laser diode was approximately 635 nm, a 0.3% difference in both cases. In fact the color camera was being used to see if the focus changed as expected with a change in source wavelength. At 450 nm there were ball foci for both the + and - 1st orders at 21.09 and 10.67 mm above the CGH, consistent with the change in wavelength of the source.

In the case of Fig. 2b, the reason there is a "hole" in the middle of the interferogram is that there were 3 other Fresnel patterns inside this one that focused 135 mm above the CGH. As the interferometer was brought toward the CGH each pattern focused at its correct location. Since the interferogram can be analyzed with aberration, or Zernike, coefficients it is possible to find focus in 3 degrees of freedom to the nanometer level as opposed to the $<1 \mu\text{m}$ level laterally and 2-3 μm axially for the ASM.

It is this precision in being able to locate the foci and the intrinsic precision of the photomask pattern to the same level that make the CGHs valuable as calibration artifacts. Further, they are made on fused silica substrates for good

thermal stability and the pattern is chrome, a coating that is stable and will take a reasonable amount of abuse without affecting its precision.

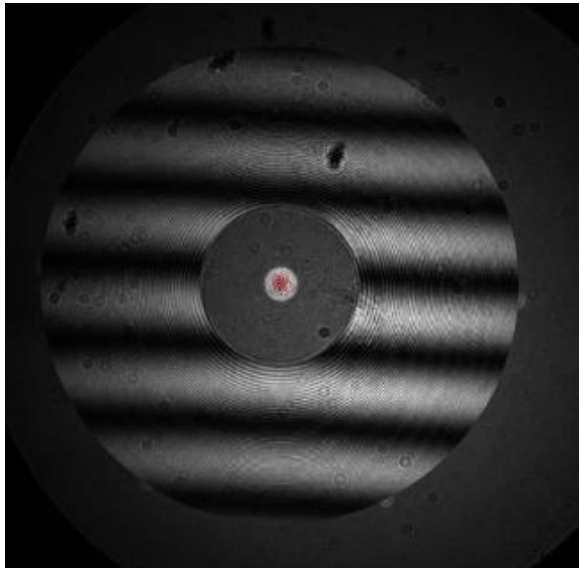


FIGURE 2b. Interference fringes from the Fresnel zone CGH pattern at the 135 mm focus. The hole in the fringe pattern is due to out of focus Fresnel patterns inside the hole.

THE AUTOSTIGMATIC MICROSCOPE

Interferometers are well known so it is obvious from the fringe patterns in Figs. 1b and 2b that there is useful and precise data in the fringes. The autostigmatic microscope is less well known so its use for interrogating the CGHs should be understood to appreciate the power of this technique. Fig. 3 shows the ray paths in a contemporary version of an ASM, the Point Source Microscope (PSM).

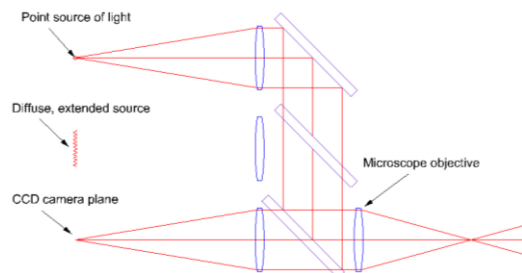


FIGURE 3. Ray path in the Point Source Microscope (PSM) showing how the point source, the focus of the objective and the focus on the camera are all conjugate in three degrees of translational freedom.

The point source is the end of a single mode fiber so the wavefront coming from the fiber is close to “perfect”. The wavefront is collimated and sent into a microscope objective via a beamsplitter to form a nominally perfect focused spot that then continues to diverge as a near perfect spherical wavefront. The focus of the objective is conjugate with the point source so that if a point source of light were placed at the objective’s focus facing back into the PSM, that light would focus on the camera at a location precisely conjugate to the original point source.

In fact, placing a point source at the objective focus is exactly how the electronic cross hairs (magenta crosses in Figs. 1a and 2a) are centered on the detector. The point source is a specular surface located at the objective’s focus so the light from the point source is reflected back to the detector using what is called a Cat’s eye image. By electronically centroiding on the Cat’s eye spot the crosshairs are conjugate with the point source. (The spots in Figs. 1a and 2a were purposely not centered on the crosshairs so the spot characteristics could be easily seen.)

MAKING THE OPTICAL CGH INTO A PHYSICAL ARTIFACT

Figure 4 shows the rays diverging from the PSM objective to a Fresnel zone CGH pattern where the spacing between the CGH and PSM objective focus is correct for rays to retrace themselves back through focus and on to the camera. Other rays are diffracted to produce a second focus beyond the CGH and the minus 1st order rays diverge back toward the PSM. This is exactly the situation for the spot shown in Fig. 2a understanding that only those rays that retrace themselves and go into the objective ever get back into the PSM to be detected.

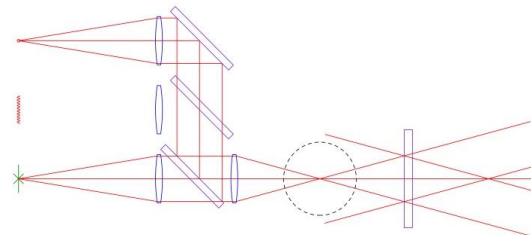


FIGURE 4. PSM ray path with a Fresnel zone CGH and a ball centered on the Fresnel zone focus. The Fresnel zone pattern also produces a conjugate focus on the far side of the CGH

Now consider the dotted circle surrounding the objective's focus. If this were a steel ball centered on the focus as in Fig. 4, the rays would strike the ball surface at normal incidence and retrace themselves back into the PSM and show up on the detector in exactly the same place as the rays from the Fresnel zone CGH pattern. This is the idea behind making the CGH into a physical artifact, an idea first demonstrated, to the best of my knowledge by Coyle³. If the pattern on the CGH produces a spot of exactly the same radius as the ball, once the PSM is aligned to the CGH the ball may be moved into place on the CGH until the return spot from it is centered on the camera crosshairs, and the ball cemented to the CGH to μm precision. Now a ball center on the CGH may be located with a tactile probe.

Since Fresnel patterns simulating various radii can be arbitrarily located on the CGH it is possible to make up fixtures for kinematically assembling optical and mechanical parts. In Fig. 5 is an example of a fixture designed for the cementing of doublet lenses without the need for a rotary table. The 3 small balls assure the convex lower half of the lens is centered with its center of curvature on the axis of the Fresnel pattern centered with respect to the 3 balls. The PSM picks up the spot from the centered pattern at the center of curvature of the concave half of the lens. Once this half of the lens is centered so its center of curvature reflection is centered in the PSM, the positive half of the lens is added and a concentric Fresnel pattern diffracts the light through the assembly to the same height so that half of the lens may be centered.

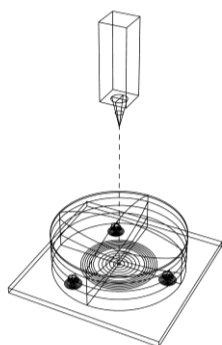


FIGURE 5. CGH fixture for cementing a doublet lens without the need for a rotary table. Lens rests on three balls symmetrically centered about two Fresnel zone patterns with radii matching the concave lower element and the same radius for the assembled doublet.

USING THE CGH FOR CALIBRATION

As a first try at designing a calibration artifact a close packed hexagonal array of 28 mm diameter Fresnel patterns were printed on a photo mask shown in Fig. 6.



FIGURE 6. A 6" photomask CGH with a close packed hexagonal array of Fresnel zone patterns on 28 mm centers⁵. The interferogram in Fig. 2a is of one of these patterns.

Since the array has an obvious 60° azimuthal period, placing the CGH on a rotary table would allow calibration every 60° . Because the outer patterns are about 100 mm from the center and the foci, or ball centers, can be located to $< 1 \mu\text{m}$, the calibration can be done to better than 1 arc second when account is taken of the doubling of sensitivity on reflection.

A more interesting use is for 3 dimensional calibrations. There are 4 concentric patterns in each 28 mm diameter patch having ball radii of 5, 15 45 and 135 mm. This CGH was placed on the work table of a CNC machine and the PSM mounted on the spindle. The PSM was centered on the central pattern and then the machine was directed to move to each ball center at the 45 mm height and at the 135 mm height. Fig. 7 shows the nominal array of 23 ball centers in light grey and the measured offsets from the ball centers at 45 and 135 mm heights in a scale that is expanded by a factor of 2000.

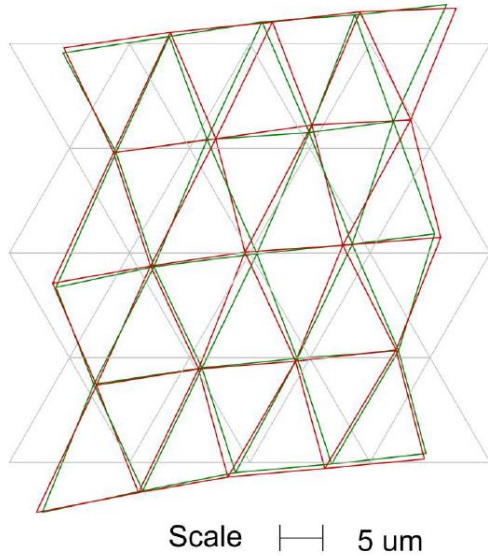


FIGURE 7. 3D calibration of a 3 axis machine tool at heights of 45 and 135 mm above the work table. The grey apexes are the nominal ball centers while the red and green apexes are errors from the nominal centers on a scale expanded 2000x.

It is clear from Fig. 7 that there was a small error in azimuth in setting up the scan, and that it appears the scale is longer in the y direction than x. Both planes look like they match to about 1 μm in most cases. We did not have time to take another set of data with the work table rotated 90° to see if the scale difference was really in the machine as expected or in the CGH⁶.

This is a rather simple example using a prototype CGH design. It is clear now that a better artifact would have evenly spaced planes in z, and longer radii in all cases. Further, the CGHs are not limited to 150 mm square substrates. It is clear that facilities exist for making photomasks as large as the largest LED TV screens, obviously with less precision but probably better precision than for any other kind of calibration artifact.

CONCLUSIONS

We have shown how simple patterns on computer generated holograms can be used as precise calibration artifacts and fixtures when they are interrogated with an autostigmatic microscope or an interferometer. The CGHs provide a flexible, stable, precise and easily

reproduced artifact that should find great utility in the field of precision engineering.

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Authors Index

Badami, Vivek	72	Kincade, John	96
Bifano, Thomas	32	Knox, Wayne	21
Billingsley, GariLynn	78	Kühne, Stefan	90
Blum, Alexander	26	Landais, Amaelle	48
Brooks, Daniel R.	21	Leach, Richard	84
Butler, Sam	21	Lynch, Tim	4
Casado, Mathieu	48	Malcher, Markus	90
Creath, Katherine	100	Marchetti, Louis	96
de Groot, Peter	72	Medicus, Kate	4
Deck, Leslie	72, 77	Olszak, Artur	9
Deisl, Florian	42	Parks, Robert	1, 104
Diez, Peter	37	Rhorer, Richard	55
Egan, Patrick	64	Ricker, Jacob	68
Ekberg, Peter	84	Riel, Thomas	15, 42
Ellis, Jonathan D.	21	Ruijl, Theo	77
Evans, Christopher	26, 77	Salsbury, Chase	9
Ferralli, Ian	4	Schitter, Georg	15, 42
Galffy, Andras	15	Sinn, Andreas	15, 42
Gandara-Montano, Gustavo	21	Soobitsky, James	77
Girard, Luc	96	Stoltmann, Tim	48
Guddei, Bernhard	37	Stone, Jack	64
Haase, Thomas	37	Su, Rong	84
Hendricks, Jay	64	Uhlmann, Eckart	90
Holmes, Michael	77	Wozniak, Kaitlin	21
Hyman, Mike	4	Yamamoto, Hiroaki	78
Jagodzinski, Marco	90	Zhang, Liyuan	78
Jobert, Nicolas	48	Zheleznyak, Len	21
Kassi, Samir	48	Zhou, Chunyu	105

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